ELECTRICAL ENGINEERING

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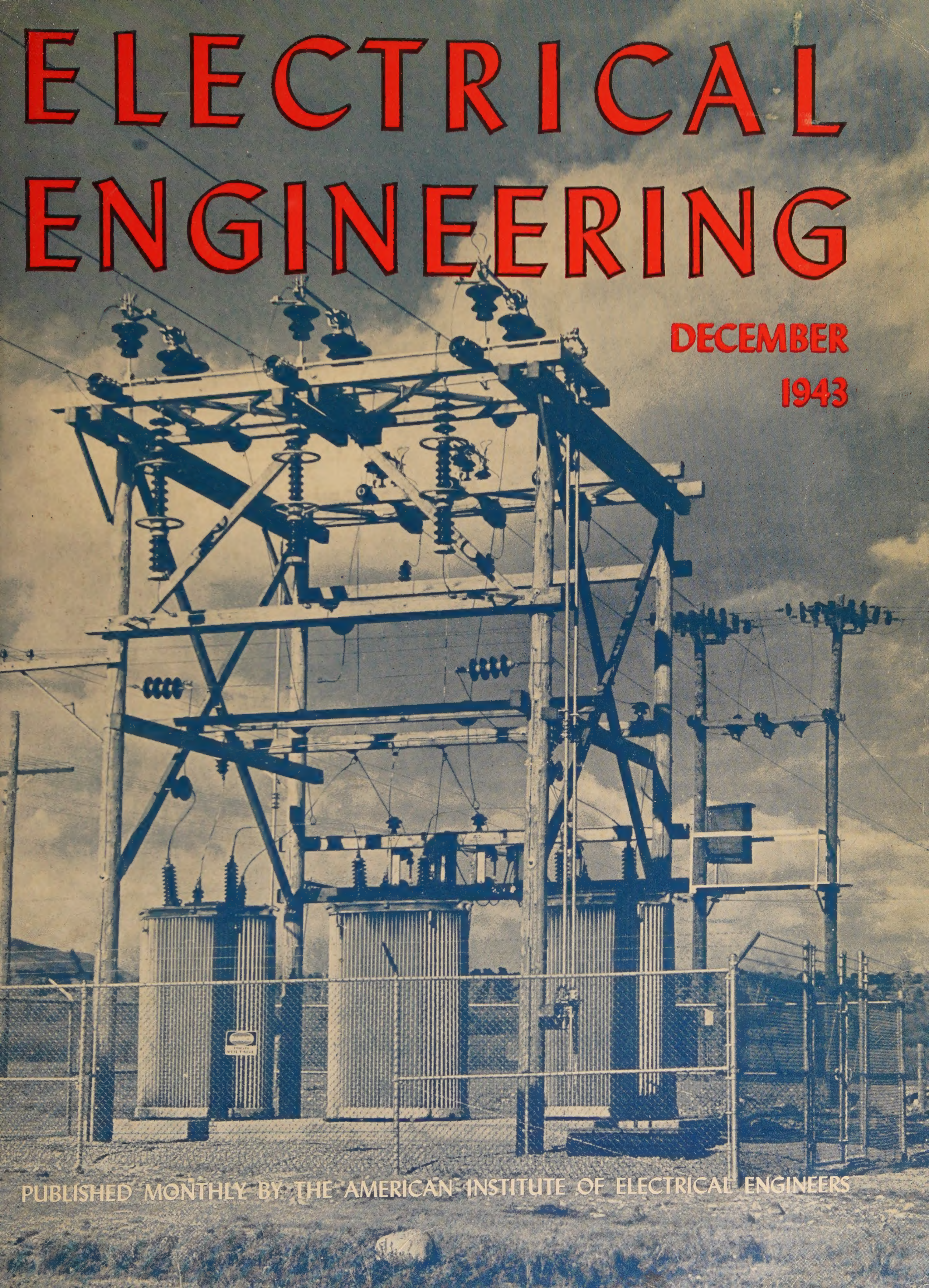


PUBLISHED MONTHLY BY THE AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

There's a Christmas rush on telephone wires, too

Help keep war-crowded circuits clear on December 24, 25 and 26.

Please use Long Distance only if it is vital.

War needs the wires—even on holidays.

BELL TELEPHONE SYSTEM

AMERICAN TELEPHONE & TELEGRAPH CO. AND ASSOCIATED COMPANIES BELL SYSTEM

United States Communications in the War

Communication facilities have been improved and expanded to meet wartime exigencies with a minimum sacrifice in essential services and speed. The way in which this adjustment has been made in the face of man-power and materials shortages makes one of the interesting chapters in World War II.

DESPITE critical shortages of man power and material, United States communication facilities in the war have been expanded to handle millions of words more every day—probably the greatest and fastest exchange of intelligence the world has ever known—with little disruption of the exchange of civilian information. It is true that radio "hams" have been restricted; singing telegrams and some other types of messages cannot be sent; and telephone calls without priority may have to wait; but these inconveniences are negligible when contrasted with the magnitude of the mobilization of the nation's telephone, telegraph, teletype, and radio services for war.

Military communications, themselves, form one of the biggest parts of the picture, and the great numbers of men enrolled in the Signal Corps and in the Naval Communications Service, as well as the vast amounts of communications equipment used by these services, in large measure account for the man-power and equipment shortages in civilian communications. The Signal Corps alone is now twice the size of the peacetime regular Army.

Despite the shutting off of many countries with which the United States was in daily communication before the war, the total volume of our overseas telecommunications has not decreased, and may have increased, since the outbreak of hostilities. Radiotelephone and radiotelegraph service to Central and South America, especially, have expanded, and modernization of those submarine cables still open permits the handling of the swollen war traffic—much of it government messages. Our chief source of news and intelligence from enemy and enemy-occupied countries is provided by the monitoring of their domestic and foreign broadcasts, and we in turn send out about 4,000 short-wave programs of news, propaganda, and entertainment a week to various parts of the world.

Our untrammeled domestic communications are being used as never before: so many people are communicating with so many others for various reasons connected with the war, as well as for the usual reasons, that long-distance telephone calls have doubled, and telegraph traffic has soared. But it is military communications which dominate the scene—not only because of their vastness, but because of the effect of that vastness on the rest of the picture.

MILITARY COMMUNICATIONS

Army communications are the function of the Signal Corps of the Army Service Forces, which as of June 30, 1943, numbered 280,000 men and 28,000 officers—twice the enrollment of the total peacetime regular army—taken in considerable part from the now thinned ranks of civilian communications workers. In addition, large numbers of communications personnel are distributed among the other branches of the Army as airplane and tank radio operators, and "walkie-talkie" and "handy-talkie" carriers and message runners. Many selectees without technical background but with high intelligence quotients have been given Signal Corps communications training in 50 military and 268 civilian schools, including schools and laboratories maintained by communications companies.

Mature men with specialized background are still needed by the Signal Corps; for example, the Corps could use all the electrical engineers and electronic physicists that it could find. As to equipment and parts, the Signal Corps, at the end of 1942, had slightly exceeded its procurement objectives.

As a result of certain characteristics of modern warfare—the great mobility of units, and their frequently wide separation from one another—radio communications far outweigh wire communications in this war even in the Army. Of the Signal Corps' $5,000,000,000 communications-equipment procurement program for this year, approximately 90 per cent is destined to be spent on radio.

Wire communications have the advantage of providing greater security: messages sent by wire cannot be intercepted or jammed by the enemy as easily as radio messages. But the difficulties of transporting wire and installing it over vast distances, in jungles, and other forbidding terrains are of course considerable. In combat theaters, wire communications are used down to the regimental echelon. Forward of that, communication is generally by radio. In making bridgeheads, Signal Corps troops usually are among the first to land. Radio communication is maintained between bridgehead and ship, and on shore wire is laid laterally and forward. By the time artillery is in a position to fire, wire communication has been established between the firing point and the command posts.

In Naval communications—the function of the Office

Material for this article was obtained from a comprehensive report prepared by the Office of War Information. The agencies and organizations consulted in the compilation of the report were: the Federal Communications Commission, War Production Board, War Department, Navy Department, Office of the Coordinator of Inter-American Affairs, Office of Censorship, Office of Civilian Defense, Post Office Department, and various labor and industrial organizations.

Western Electric photo

Communications provide the essential link which co-ordinates every unit in a combat theater, and enables the Armed Forces to make a simultaneous joint attack upon a given area from the air, the sea, and on land. This artist's drawing shows seven ways in which communications aid operations during combat

In (1), field headquarters is receiving orders from general headquarters through electric communications. In turn, it sends orders of its own through field telephones, teletypewriters, switchboards, wire, cable, and radio. The commander of the Air Force (2) uses radio to transmit orders to his squadron. Transport ships (3) employ battle-announcing systems to permit speedy action on commands. *PT* boats preceding the transports (4) also receive their orders by radio. In (5) the telephone is used by observation posts for the artillery to transmit messages, and by the artillery stations (6) to reply to the observation posts and to field headquarters. Tanks (7) shown here on a flanking maneuver, are followed by troops in personnel carriers. Tank commanders co-ordinate their actions with other units by radio

of Naval Communications—wire naturally plays even less of a role than in the Signal Corps, and the Navy's use of radio communications is proportionately higher.

The Coast Guard has leased five of the radiotelegraph stations on our coasts and operates them in maintaining distress watch for ships. Radio is used by the Navy not only for long-distance communications but also for short-range work between the ships and planes of a modern task force. At sea, in addition to the usual radio work, the Navy makes use of devices for the detection of enemy ships, planes and submarines. The many communications activities of the Marine Corps do not differ essentially from those of the other branches of the armed services.

Total radio production in this country which about a year ago stood at a level of $30,000,000 a month, is now up to $250,000,000 a month,—a considerably greater rate of increase than that of total war production. All such production is for the armed services, which use many other radio products besides radios for tanks, aircraft, battleships, cruisers, submarines, destroyers; field sets for the Army; public-address systems; radio compasses; direction finders; and altimeters.

In the majority of the military sets being made, receiver and transmitter are associated. Every combat tank and airplane is equipped with two or more complete communication sets. Short-wave communication is maintained between tank commanders and the individual tanks under their control, and planes communicate constantly with each other.

Some radio products are still secrets of war, and constant new developments not only have to be met by widened training in operation and maintenance, but are responsible in turn for whole cycles of research and development (usually by private companies). The war has brought about a markedly increased use of "walkie-talkies" and "handy-talkies" in various military operations on the firing line, and in intrafleet and intra-air squadron communications.

DOMESTIC COMMUNICATIONS

Domestic Standard-Band Broadcasting. During 1941, the last normal year of production, about 13,000,000 domestic broadcast-receiving sets were manufactured, and on April 22, 1942, when production ceased in favor of manufacture of military equipment, several million sets remained in the hands of manufacturers and dealers. There are still certain models on hand for purchase, all of which are well over a year old. If automobile sets are included, there are 60,000,000 receiving sets in the United States—or approximately one set for every two inhabitants. Since there are only about 31,000,000 "radio families" in the country, it is apparent that a goodly number of American families own more than one radio with which to listen to the 900 or so standard broadcast stations sending out programs almost continuously.

Many domestic receiving sets are deteriorating from age and lack of adequate service; the greatest difficulty at the present time is the securing of tubes. During the early part of this year large numbers of tubes destined for civilian use were taken over by the armed services, and, though the civilian program is being pushed, the supply is still tight. This is true particularly of tubes for the a-c–d-c sets sold in such large quantities just before the war which, fortunately in most cases, seem to be extra radios in homes.

The chief bottleneck in the manufacture of tubes is not in material but in labor; a number of manufacturers are now setting up feeder plants for tube making in areas where labor is available. The production of batteries has recently been increased to take care of the estimated 3,200,000 battery sets on farms in non-electrified areas.

The Federal Communications Commission (FCC) has made an inventory of excess radio equipment in the hands of radio stations throughout the country available for purchase by other stations. Catalogues listing this equipment and its location can be consulted by stations wishing to buy equipment directly from other stations, enabling them to avoid placing orders with manufacturers swamped by war orders.

At the suggestion of the War Production Board, FCC issued an order under which all domestic broadcast stations, without disturbance of service, have effected operating changes as a wartime means of extending transmitter-tube life. WPB simplified and standardized parts for home radios and similar equipment in order to assure wider maintenance and repair. The FCC, in conjunction with the Board of War Communications (BWC) and the Army, also has made detailed arrangements to silence any radio station in danger of being used by enemy aircraft as a radio beacon.

Man-power shortages are severe in the manufacturing, maintenance, and in the broadcasting ends of radio. Young men have been particularly predominant among the employees of the industry, and the need for their services in the Signal Corps and Naval Communications has led great numbers of them to enlist. Others, although eligible for draft deferment because of their employment in essential communication jobs, have not accepted deferment, and in some cases local draft boards have not granted it.

Despite the shortage of personnel and the tightness of equipment, domestic radiobroadcasting continues in its normal channels. The extent to which the war has affected program content is familiar to all, and, indeed, it is in certain radio programs that large sections of the American public most frequently hear the collective voice of the men at camp.

In addition to performing much wartime research in its laboratories, the radio industry has contributed much time and talent to the broadcasting of government war messages—about $140,000,000 worth during 1942.

Broadcasting stations and radio programs are responsible in large part for public understanding and acceptance of such measures as gasoline rationing, point rationing, and the Victory tax; and for the success of such campaigns as those for the use of V-mail, the purchase of War Bonds, and recruitment of glider pilots and student nurses. Every station in the country has been making between 9 and 12 announcements of war messages a day, from material furnished by various government agencies through the Office of War Information, which acts in a co-ordinating capacity.

Among the stations contributing this wartime service are the 170 domestic foreign-language stations; these broadcast programs in 30 foreign languages for a total of approximately 1,500 hours a week, and are aimed at the 25,000,000 inhabitants of the United States, most of them American citizens, who speak at least one foreign language. Of this number, there are 11,000,000 whose primary language is not English, and 2,000,000 who neither speak English nor understand it.

The preponderance of music is higher in these programs than in English-language programs, but otherwise their content of news, drama, and feature material is much the same. The chief languages are Italian, Polish, and Spanish, and the stations are heavily concentrated in the northeastern and north central states, with a number in the southwest and far west, also. There are practically no foreign-language stations in the South and the Northwest.

Before Pearl Harbor, a considerable amount of blatantly antidemocratic and pro-Axis propaganda had gone out over some of the foreign-language stations. The three existing Japanese-language programs went off the air voluntarily when the United States entered the war, but in certain other programs spokesmen continued to employ intonation, inflection and selection of news items to put across their anti-American views.

Today, three groups are concerned with maintaining foreign-language broadcast security: the Federal Bureau of Investigation, which checks personal-history statements and fingerprints of all persons engaged in production or presentation of foreign-language broadcasts; the Office of Censorship, which administers a voluntary "Code of Wartime Practices" for the stations; and the FCC, whose interest is in the operation of licensed stations in the public behalf. The stations, themselves, are responsible for all material which they broadcast, and censorship is thus on a voluntary basis, exercised either by the individual stations or through an industry committee, the Foreign Language Radio Wartime Control.

Television, Facsimile Broadcasting, and Frequency-Modulation Broadcasting. The development of television, facsimile broadcasting, and frequency-modulation broadcasting has been considerably affected by the war: in each case postponement of widened service has been forced by shortages of materials and man power. Facsimile broadcasting is still restricted to commercial use only, in connection with private point-to-point operations, but when its development and the development of frequency-modulation broadcasting are resumed, the two doubtless will be carried on jointly on a large scale. In the case of television, the FCC had limited the licensing of transmitters to prevent the freezing of the technique at a low level of effectiveness. Also, many new advances resulting from laboratory experimentation in connection with military devices will improve transmission and reception when television is allowed to develop on a commercial basis again.

Domestic Nonbroadcast Uses of Radio. Domestic nonbroadcast uses of radio are many, including use by police and fire departments, by forest-fire services, by exploration companies searching for oil or minerals, by coastal harbor stations for communication between ship and shore (now limited because of the wartime necessity for ships off shore to observe radio silence), and, on a huge scale, in aeronautics.

Since our entry into the war, certain plants manufacturing munitions and other materials have been permitted to operate their own radio stations for the purpose of communication in case of emergency. The War Emergency Radio Service, organized under the administration of the FCC with the co-operation of the Office of Civilian Defense, comprises several thousand of the country's licensed radio operators, including many of the licensed amateurs. (The operation of all amateur stations, approximately 60,000 in number, was suspended on December 7, 1941.) These operators stand ready to substitute their very high-frequency radio communication for wire service; or to supplement wire service in case of enemy bombings, other military operations, or such emergencies as flood, hurricane, or earthquake.

A number of the FCC's radio activities are directed toward the maintenance of safety at sea, a particularly crucial factor in wartime. The Commission has set up special requirements for receivers to be used on board United States vessels, and gives its approval only to those types of receivers which do not radiate signals that could attract the attention of enemy raiders. FCC men guard the special frequencies which ships use to send SOS (ship sinking) and SSS (submarine sighted) signals. When such a signal is heard the Anti-Submarine Command of the Army Air Forces is notified, and planes are on their way to the scene, sometimes within five minutes.

The routine "frequency measurement" work of the FCC has a direct bearing upon safety at sea. (This consists of checking with the utmost accuracy the exact frequency being used by various transmitters against the frequency assigned.) For example, a vessel struck by a torpedo sends out an "autoalarm" signal on the 500-kilocycle channel, and this signal automatically rings alarm bells in other vessels and at marine watch stations. If the ship's transmitter is "off frequency",

the automatic alarms on other ships may not ring and the message may go unheard.

Domestic Telephone. There are about 26,500,000 telephones in the United States—17,000,000 of which are residence phones. About 5,000,000 of them belong to the 6,300 independent telephone companies and the 60,000 rural or farmer lines; the rest belong to the Bell Telephone System. At present, it is expected that there will continue to be sufficient instruments available to take care of all essential telephone users as defined by the WPB, and to provide for what the Board calls "essential growth." If he is not on the list of essential users, however, an American who lives in one of certain areas where telephone-exchange facilities are loaded to the limit and materials are not available to enlarge them further, can no longer count on having a telephone installed promptly in his home or place of business. As in the case of radio receiving sets, the production of new telephone instruments for civilians has been stopped in favor of the manufacture of equipment for the armed services, and old-fashioned "desk" sets are being brought back into home use.

In some areas service is being "de-graded"—that is, only party-line service is being installed, and in some cases, substituted for single-party service. Both the Bell Telephone System and the independent telephone companies have expended considerable effort in conserving strategic war materials and in undertaking valuable research, and they are maintaining extensive advertising campaigns to discourage unnecessary use of the telephone that might hinder the war effort.

Figure 1. A midget Western Electric lip microphone concealed in the nozzle of his oxygen mask and earphones carried in his helmet enable this pilot to send and receive messages while his hands remain free for fighting. Similar microphones are used, also, by the Army ground forces

In the same way that users of essential service are given precedence in obtaining telephone facilities, certain urgent long-distance calls are also given priority. By order of the BWC, three classes of priority have been set up for long-distance calls vital to the war effort. Long-distance telephone traffic has nearly doubled in the last two years. At the present time, about 2,200,000 long-distance telephone calls are being made in this country per day, at an average connection speed of 3.7 minutes. Some idea of the crowding of the circuits may be gathered from the fact that two years ago the average connection speed of long-distance calls was $1^1/_2$ minutes. On the other hand, in 1916, just before this country entered the last war, average long-distance connection speed was 16 minutes. Toll calls out of Washington, D. C., alone, now average 42,000 a day, as contrasted with 24,000 a day just before Pearl Harbor.

On a country-wide average, long-distance calls still are made more frequently during the day than at night. However, in areas where there are large military camps, night calls, from 7 p.m. to 10 p.m., often are more numerous. Servicemen at the camps are usually off duty at about the same hours and consequently place their calls at the same time. This places such a burden on the circuits that delays on calls from camp pay stations often are unavoidable. In an effort to make the situation as comfortable as possible, the telephone companies now provide "attended service" at most of the larger camps and bases. The soldier or sailor can give his call to an attendant and then relax, knowing that he will be called as soon as his connection is ready.

Considerable flexibility in long-distance service is made possible by the long-distance traffic control bureaus of the Bell Telephone System, which shift circuits into localities where they are most needed at a particular time. At New York City, alone, changes of this kind are made almost 200 times a day. Some circuit shifts are planned in advance, such as the nightly shifting over of as many circuits as possible from normal business day usage to serve Army camps. Generally speaking, however, the demand for long-distance service is anything but uniform, with sudden, unpredictable surges often occurring where least expected. Whenever possible, the BWC urges the public to make its long-distance calls during off-peak hours and on Sundays.

Applications to the FCC, the BWC, and the WPB by telephone and telegraph companies for permission to expand service are carefully examined to determine whether they will fill war needs so that the use of scarce equipment and material for nonessential purposes will not result.

The telephone companies have done a good job in substituting women for men among their employees. It is estimated that seven tenths of all Bell Telephone System workers are now women, as are at least 55,000 out of the 75,000 employees of the independent companies. Even so, there are severe man-power shortages

throughout the industry, in the independent companies particularly. Traditionally low wages do not help the situation.

The over-all telephone picture thus is one of crowded circuits and of man power and material shortages, but one also of a continuation of all essential and all non-essential civilian traffic as well, except on such an abnormal day as Christmas, when the long-distance connections desired mount to a dizzying number.

Teletypewriters and Teleprinters. The use of teletypewriter or teleprinter systems, by which messages typed on one instrument are instantaneously recorded on others connected with it by wire, or occasionally by radio, has vastly expanded under wartime conditions. Government and industry are at present the greatest users of these services. The press comes third. Teletypewriter links across the country, including both leased-line and exchange services, now total nearly 2,000,000 circuit miles for the Bell Telephone System alone.

A teletypewriter exchange service makes it possible for one subscriber to be connected with another as rapidly as a long-distance telephone connection can be made. Other wire companies provide substantial additional mileage for private teleprinter systems. The railroads and pipe-line companies also make extensive use of teleprinters on their own wire lines. The Civil Aeronautics Administration leases a teletype circuit of about 63,000 miles for its aviation weather-reporting and traffic-control services. The FBI, likewise, is a big user of the teletype.

The supply of these "typing by wire" machines is far from adequate. They are being manufactured at the rate of 20 times their production before the war, but the volume of Army teletypewriter orders on hand at the present time equals approximately the total number of machines owned by the Bell Telephone System.

Domestic Telegraph. Like long-distance telephone calls, telegrams are now on a priority basis. Several classes of priority have been set up, and both private and government telegrams of direct importance to the war effort take precedence over messages not relating to the war, if such priority is requested by the sender. Formerly government messages had always been given priority, pursuant to an act of Congress.

The establishment of these priorities by the BWC was one result of an investigation of telegraph service undertaken last year by the FCC at the Board's request—an investigation carried on in 12 large cities with the co-operation of the two telegraph companies and labor unions. The investigation showed that speed of telegraph service in the United States had deteriorated considerably. In many of the cities checked, more than half the messages studied were found to be still undelivered an hour after filing, and in some cases, more than a quarter of them remained undelivered at the end of an hour and a half. Because of the presence of many new war plants and increased port activity, cities on the west coast were getting the worst service. Even war traffic in some cases was being delayed badly.

In addition to setting up priorities, the BWC issued orders establishing speed-of-service goals for telegraph messages, discontinued all nontelegraphic services, and banned the acceptance of domestic congratulatory and greeting telegrams. Earlier, the companies, themselves, had canceled all fixed-text social messages and singing telegrams.

Western Union figures for May 1943, the latest available, state that the average origin-to-destination delivery time for top priority telegrams had been reduced within the past year to 23.06 minutes, and for ordinary government and commercial telegrams, to 36.04 minutes. Preliminary FCC studies indicate that, whereas speed of service has improved since its 1942 investigation and extended delays have been reduced, the BWC goals for telegraph service are not being met yet.

Labor shortage is apparently at the heart of the problem. Traditionally low wage scales in the industry have made the present man-power situation especially acute. In January 1943, the WLB found Western Union "messenger quits" in the New York area to be running at the rate of 340 per cent a year. Elderly men and women are now being employed successfully by Western Union as messengers, and in certain cases a bounty is given to an employee of the company for bringing in another employee who remains a certain length of time. Recent national WLB awards made in the case of both Western Union and Postal Telegraph have raised wage rates substantially in both companies, and should be of great assistance in attracting employees and holding them within the industry.

On June first of this year Western Union monthly receipts showed an average increase of 70 per cent over January 1940. Some of the greatest expansions have been in private-line service between defense plants and government agencies, and in tie lines in defense areas. In general, much of the growth is due to increased business and widespread absence from home; there have been many new installations of service at camps. Members of the United States armed forces and persons sending money to them receive a substantial reduction in domestic-telegraph money-order charges. Like the telephone companies, the telegraph companies also are engaging in considerable laboratory research of benefit to military communications.

As contrasted with telephone business, most of which is local or intrastate, 95 per cent of telegraph business is long haul or interstate. Consequently, overnight telegraph business has suffered greatly during the past decade from the competition of airmail deliveries. It has suffered also from the competition of long-distance telephoning, since these rates have been decreased substantially.

The hope for continued large-scale existence of telegraph business would seem to lie in an increased mecha-

nization, which will lower costs and thus permit effective competition with other forms of rapid communication. Messenger boys will doubtless occupy less than their present place in any large-scale postwar communications system.

INTERNATIONAL COMMUNICATIONS

International Broadcasting. All 14 of the country's privately owned short-wave broadcasting stations (one of them built since Pearl Harbor), and eight commercial communications transmitters formerly used for point-to-point telephone, program, or radiophoto service, have been leased jointly for the duration of the war by the Office of War Information and the Coordinator of Inter-American Affairs (CIAA). Twenty-two new transmitters are now being added, and on their completion, the considerably lower-powered commercial transmitters will be dropped.

The programs which the two federal agencies broadcast internationally constitute an integral part of American psychological warfare. OWI's international message, the "Voice of America," is aimed at five listening groups: enemy areas, Axis-occupied areas, neutral countries, the United Nations, and Allied-occupied areas. It is heard 24 hours a day in more than 40 languages and dialects in a total of more than 3,200 quarter-hour productions a week.

To supplement this coverage, more than 100 programs a week are picked up by the British Broadcasting Corporation and rebroadcast by medium wave. Also, many transcribed programs are shipped abroad. Some of the "Voice of America" programs are developed by the National Broadcasting Company and the Columbia Broadcasting System from scripts prepared by various government agencies for which the networks provide the talent and the direction.

There are many indications—some of which must remain secrets of war—as to the effectiveness of the "Voice of America" in combating Axis propaganda and spreading the meaning of our cause. The day after the invasion of North Africa, for example, when it was of paramount importance to reach French listeners in North Africa and France, the Berne correspondent of the New York *Times* cabled his paper: "American broadcasts are listened to day and night, and it is certain a great impression has been made. The French may be skeptical but they are also sentimental, and President Roosevelt's reference to 'France eternal' dimmed many an eye."

Programs in French are broadcast 22 out of every 24 hours. Reception is generally very good throughout France, and a London intelligence source, familiar with French underground papers, estimated recently that at least half of their news and feature items were taken from United States broadcasts.

A dispatch from Stockholm on May 8, 1943, to the Washington *Star* declared: "Paul Joseph Goebbels has been fighting a losing battle here, where American propaganda has slowly overwhelmed the Nazis' energetic and carefully planned effort to convince the Swedes that Germany has right on her side and is bound to win the war."

In French Guiana and Martinique, OWI broadcasts were an important influence in bringing about the downfall of the pro-Vichy governors and the alliance of the colonies with the United Nations. From the angry reaction of Axis home radios, which frequently go to great lengths to answer broadcasts from the United States, it is apparent that OWI has numerous listeners in Germany and Italy. There are probably fewer in Japan, but even thence comes evidence that some Japanese, both at home and overseas, listen to America's "voice."

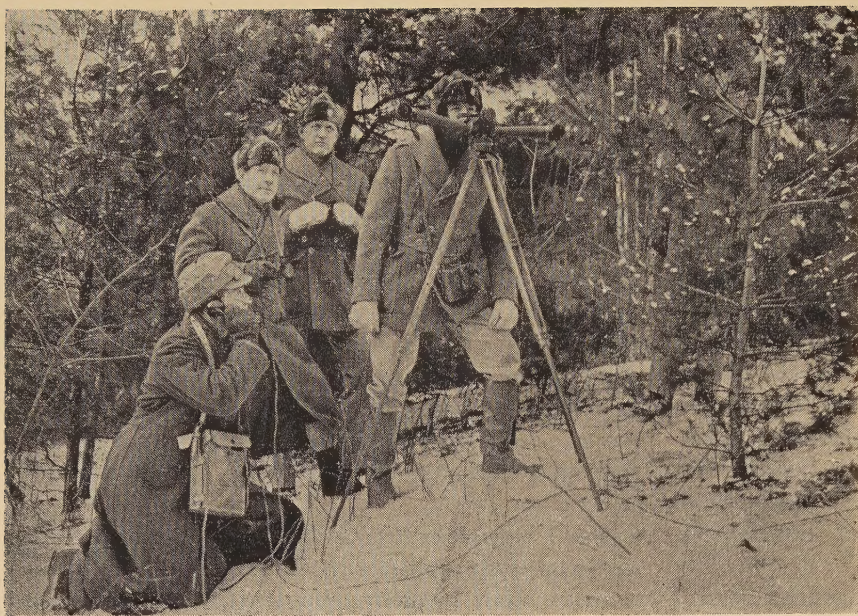
Since November 30, OWI has maintained constant two-way radio contact with Algiers; and North African stations, both medium- and short-wave, relay many United States programs to Europe on a daily basis.

In this hemisphere, OWI serves the three South American colonies of foreign powers and the West Indies colonies. The CIAA, on the other hand, works with all 20 of the independent Latin-American republics, sending out a total of 550 short-wave programs a week, which range in length from five minutes to half an hour. Of these, 153, which are aimed at Brazil only, are in Portuguese, 186 are in English, and 211 in Spanish. Most CIAA programs are produced by NBC and CBS, under contract.

Since its programs do not go to enemy or enemy-occupied countries, where short-wave broadcasts are the only means of communication, CIAA is able to use other forms of communication to a greater degree than OWI: some of its programs, for example, are sent out of the United States by radiotelephone and rebroadcast locally, and many others are produced locally in the Latin-American countries.

CIAA makes greater use of transcription than does OWI. Its short-wave broadcasting is done over the same stations used by OWI, but its peak of activity comes at a different time; 5 p.m. to midnight, Eastern War Time, OWI's slack period, is CIAA's period of greatest activity. CIAA broadcasts about eight hours a day. Its listeners abroad are not furtive, like OWI's. They can listen as long and as openly as they please. It is possible to make the average broadcast longer, to send over the air large-scale examples of our popular and intellectual culture, and to familiarize our Latin-American neighbors with large aspects of our lives and institutions.

For about an hour each night, CBS and NBC networks of local Latin-American stations—150 stations in all—pick up CIAA broadcasts on time paid for by CIAA, and carry them to vaster audiences than can be reached directly by short wave. Surveys show CIAA programs to be well received by large audiences. CIAA



Western Electric photo

Figure 2. A field telephone being used to direct fire-control commands on maneuvers in northwest United States

cast Intelligence Service (FBIS) of the FCC covers about 2,500,000 words a day, summarizes and digests the broadcasts, records the more important of them, translates them from 35 or more languages and dialects, and finally sends on the intelligence which they contain to the government departments concerned. It is thus possible to know the enemy's propaganda lines to their own people, to neutrals, and to the United Nations, and to neutralize or combat them. The OWI, for example, checks the FCC interceptions of German short-wave propaganda against what the Germans are saying to their own people, or with contradictory statements which prove the falsity of the stories that they broadcast to America.

broadcasts news every hour on the hour, sending out a total of three hours of news a day. News commentators from the Latin-American countries, stationed permanently in New York, broadcast regularly.

In general, CIAA's broadcasting schedule bears a greater resemblance to a domestic network than does OWI's. There is a good deal of music and amusement, and less of the special intense kind of information and encouragement called for by the living conditions of much of OWI's audience. Each of these agencies fits its programs to its special job. Like OWI, CIAA broadcasts programs of news and entertainment to our servicemen overseas.

Foreign Broadcast Intelligence Service. Although a certain number of Americans—especially Americans born abroad—listen to short-wave programs broadcast from foreign countries, the total American audience for such broadcasts is not large even in wartime. Americans appear to be satisfied with the variety offered by domestic programs and to prefer the quality of reception on local stations. Nevertheless, regardless of how many Americans make a practice of listening to them, short-wave broadcasts from Germany come to this country 11 hours every day and from Japan $4^1/_2$ hours. Other short-wave programs come from our Allies, our Latin-American neighbors, and many other countries.

Because the enemy and enemy-conquered countries have cut off the regular channels of rapid news communication (diplomatic staffs, press representatives, cable news service, travelers), enemy and neutral radiobroadcasts for domestic and foreign consumption are our chief source of foreign news and intelligence. To keep government agencies and the armed services informed of the contents of these foreign broadcasts, the Foreign Broad-

The FBIS divides the world into three parts:

1. Broadcasts from Asia and the Far East are listened to from Portland, Oreg., and San Francisco, Calif.
2. Friendly broadcasts from Latin America are received at Kingsville, Tex.
3. International broadcasts from Europe, Africa, and the Middle East are intercepted in Washington, D. C., and Puerto Rico, and home broadcasts are heard near London, Eng.

The FBIS forms part of the widespread United Nations network of radio interception, co-operating especially with OWI and with British and Dominions interception services all over the world with interchange of findings. The FBIS monitors for the Army all messages from American prisoners of war forwarded by Axis radio. These are forwarded to the War Department for notification of relatives. Details of the bombing of Tokyo—announced on Japanese broadcasts to home listeners—were first learned over the monitoring radio in the Portland, Oreg., receiving station.

Submarine Cable. Although the war has interrupted cable communication services of American companies to continental Europe and to Far Eastern points, direct facilities are still available to the United Kingdom, Eire, Portugal, Gibraltar, the Azores, Hawaii, and Midway. In addition, there is complete Western-Hemisphere cable service, uninterrupted by the war, to the West Indies and along both coasts of South America; and the submarine cables to Alaska have been modernized to carry heavy traffic.

About 66 per cent of international-communication telegraph traffic to Europe, the Near East, and Africa is handled by cable, as is about 81 per cent of the traffic to South America and about 30 per cent of trans-Pacific traffic, including traffic via British cables from Canada.

(The reason for this low percentage of trans-Pacific cable traffic is that only two direct cables exist to Australia—those from Vancouver; all other trans-Pacific cable traffic, except for the American cable to Hawaii and Midway, must go via the Mediterranean Sea or the Cape of Good Hope and India.) The rest of the traffic is handled by radiotelegraph.

In general, it may be said that, computed by the number of messages, 65 per cent of the world international communication telegraph traffic is handled by cable and 35 per cent by radio. The speed of the two services is the same between points to which there are direct cable circuits; when relays are necessary, radio is faster.

One of the reasons for this continued predominance of ocean cable in the face of radio competition is habit—cables have always carried the bulk of direct business traffic. On those lines which are still open, business has greatly increased, and the traffic is largely in government messages with a priority known as "government urgent." The State Department, the Army, the Navy, and other government departments lease their own time on cable circuits, some of them for 24 hours a day, and others part time. The adoption of the varioplex channeling system—widely used on land telegraph lines—on the cables to England makes it possible to send as many as 12 messages simultaneously over a single cable.

Another reason for the continued popularity of submarine cable is the secrecy which it affords in time of war. It is difficult to tap an ocean cable. Plans exist by which increased radiotelegraph facilities will be made available in case of the cutting of any of the cables by the enemy. Cable repairs, difficult in the best of times, present increased hazards at present. Not only are there shortages of many materials needed in repair work, but also cable-repair ships now require naval escort. Several British cable ships (most cable ships are under British registry) have been sunk in the North Atlantic while engaged in their work or en route to it.

Radiotelegraph. About the middle of 1942, all domestic radiotelegraph traffic, with the exception of the transcontinental circuits which handle the relay of international traffic, was ordered discontinued by the BWC because domestic messages carried by this medium could be intercepted easily by other countries. Actually, only about $1^1/_3$ per cent of domestic telegraph messages had ever been carried by radio, and parallel service was offered by the wire telegraph.

Although the entry of the United States into the war brought about the discontinuance of direct radiotelegraph circuits with Axis countries and countries occupied by the Axis, international radiotelegraph service to and from the United States has been extended greatly since the beginning of the war. Prior to Pearl Harbor, for example, radiotelegraphic communications between the United States and Australia were relayed via Montreal. Now the traffic is routed over direct circuits. Since 1939, new direct radiotelegraph circuits have been established to Egypt, Iceland, Paraguay, Bolivia, New Caledonia, Greenland, New Zealand, Iran, French Equatorial Africa, Belgian Congo, Algiers, British Gold Coast, Bermuda, Afghanistan, and to numerous points in the European and Asiatic Union of Soviet Socialist Republics and unoccupied China.

To each new point to which service is inaugurated, only one company is permitted to operate. Shortages of materials and the limited number of long-range channels available for international communications rule out parallel, competing circuits. Civilian use must be held to a minimum if the greatly increased number of essential military services is to be handled in the already crowded radio spectrum.

Expeditionary Force Message Service. In June 1942, special low-priced cable and wireless message rates were made available to members of the American Expeditionary Forces and persons communicating with them. A combination of any three of 104 fixed texts, designed to cover nearly all occasions, offers the sender a low message rate—60 cents or its equivalent in other currencies. This is a two-way, purely overseas service. It is available at practically every overseas base.

Overseas Telephone. Before the war, the United States was connected by direct radiotelephone service with all major countries and strategic areas throughout the world. The first to be shut off from us were the German-occupied countries in Europe. Then, after

OWI photo

Figure 3. This lookout station banked with sandbags shelters one of the blinker lamps used in United States coastal defense to guide incoming ships to shore

Pearl Harbor, service to Berlin, Rome, and Tokyo was suspended, and subsequently, service to Java and the Philippines was discontinued when these countries were occupied by Japan.

During the past year, radiotelephone service has been extended to Surinam, Dutch Guiana, and to Recife, Brazil, via Rio de Janeiro. Plans for service to the Soviet Union and to several additional islands in the Caribbean are under way. Because of the tremendous increase in radiotelephone traffic to Central and South America, Hawaii, Puerto Rico, and Panama, additional circuits have been established to those points. Radiotelephone traffic between the United States and Panama has augmented more than 200 per cent since Pearl Harbor, and between the United States and Hawaii, over 300 per cent. This growth in inter-American calls has more than offset the loss of calls to other parts of the world. Today the total radiotelephone-message volume is about half again as large as before Pearl Harbor.

Except for the Bahamas and Jamaica, personal radiotelephone calls may be made between any two points in the Western Hemisphere. For the rest of the countries with whom telephone connections are still open, however, the following BWC regulations are in effect:

1. Except for American press calls and radiobroadcast programs, no nongovernmental business or personal radiotelephone call can be made to or from any foreign point outside the Western Hemisphere other than Great Britain, or to and from the Bahamas or Jamaica, unless the call is made in the interest of the United States or the United Nations, sponsored by an agency of the United States government and approved by the Office of Censorship. Nongovernmental business or personal radiotelephone calls are not permitted between the United States and Great Britain.

2. No calls of any nature over the radiotelephone circuits under the jurisdiction of the United States, no matter where such calls may originate, unless they are sponsored and approved as indicated in paragraph 1, shall be permitted to, from, or on behalf of the following 13 countries: Egypt, Finland, France, Iceland, Iran, Ireland, Latvia, Lithuania, Portugal, Spain, Sweden, Switzerland, and Turkey.

To prevent overseas telephone conversations from being overheard by curious listeners, voices are "scrambled" by various radio devices as they go over the circuits and "unscrambled" when they arrive at their destination. Ship-to-shore telephones on ocean-going vessels are no longer employed for public use as they would betray ship locations to the enemy.

POLICING THE ETHER

Since the early days of radio regulation, monitoring has been necessary to make sure that radio transmissions obey ordinary ether-traffic rules. Various types of radio transmissions are assigned particular ether lanes in which to travel. If one signal strays over its assigned "white line," there is collision with other services and resultant confusion. Likewise, if a transmission appears in the ether paths without identifying call letters, it is as quickly spotted as an automobile without license plates traversing a land highway.

At present, the Radio Intelligence Division of the FCC is particularly alert for signals which might prove to be illegal. Since July 1940, over 2,000 cases have been investigated and many operators have been convicted. Also detected have been many radio circuits operated by enemy agents.

For its policing work, the Radio Intelligence Division maintains 12 primary monitoring stations, 90 secondary monitoring stations (one or more of which are located in each of the 48 States and in all territories and possessions), and three radio intelligence centers at Honolulu, San Francisco, Calif., and Washington, D. C. Monitoring stations are usually located in isolated places far from the nearest town in order to procure ideal listening conditions.

Furthermore, 30 mobile units of two men each now maintain a continuous automobile patrol of the entire 5,000-mile coast line of the continental United States. These coastal units are particularly on the watch for any radio transmitters on shore which might be communicating with an enemy ship at sea relative to the departure, location, or cargoes of departing vessels. The system is so organized that a clandestine signal receivable anywhere in American territory can be traced.

When an intruder is detected in the ether lanes, either by one of the Division's monitoring stations or by a broadcaster who reports it, direction-finding apparatus is called into play. Three or more monitoring stations collaborate in getting a bearing on the signal in question. Their beams are plotted on a map, and eventually the lines will cross. This point of intersection, marks the general location of the origin of the transmission.

The final task of running down the offender is performed by monitoring officers—men highly skilled in radio technique—using automobiles which are fitted with the latest and most efficient type of detection equipment, including direction finders, all-wave receivers, and recorders. All this apparatus can be operated from the car's battery, or, upon being removed from the car, from the power supply of a dwelling, store, or tourist camp. Operation of the mobile equipment follows much the same procedure employed by the monitoring stations in the first instance. Bearings finally fix the exact location of the transmitter in question. Even if the hunt narrows to an apartment house, hotel, or other large building, a monitoring officer, by using a device carried in his hand or in his pocket, can proceed from floor to floor and from door to door until he determines the exact room in which the equipment is being used.

CONCLUSION

The impact of vastly expanded military communications, swollen civilian wartime traffic, and other purely wartime considerations is to be felt in every branch of communications. Man power, materials, service, and as regards broadcasting, program content, all testify to the fact that "this is war."

Metallurgical Problems Arising From the Use of Copper in Electric Equipment

E. R. PARKER

Pure copper is used extensively in electric equipment because of its high conductivity, but its low tensile strength and its high specific gravity make it generally unsuitable for structural purposes. The author describes the problems involved in adjusting the various properties of copper so that the greatest efficiency in the use of the equipment can be achieved.

COPPER has played the leading role as an electric conductor ever since the inception of the electrical industry early in the 19th century. Galvani in 1786 used copper when he noticed the curious behavior of frog's legs hung by copper hooks from an iron railing. His experiments excited much interest and led to the development of the first electric battery by Volta in 1799. Volta's battery employed copper as an electrode material. After 1800, the study and application of electricity made rapid progress. By 1809, John Children had built a battery with 20 pairs of copper and zinc plates, each over 15 square feet in area. With this battery he measured the conductivity of various materials, and proved that copper was superior as an electric conductor.

One of the earliest practical applications of electricity was for communication. In 1837 the first telegraph was put into commercial use. Greater power than was available from batteries was needed for the extensive commercial applications. The electromagnetic investigations then in progress led to the discovery and development by Faraday of the first generator, which he built in 1831. As a result of Faraday's work, power-driven generators were in commercial use within a few years. Since then, the electrical industry has grown to such an extent that huge machines capable of generating 200,000 kva are now in use.

In normal times the electrical industry is the largest user of copper. Table I gives the estimated use of copper in the United States for the period 1936–40. These figures include all production and use of either pure or alloyed copper. About 35 per cent of the total copper production is consumed by the electrical industries. Because of the large use of copper in electric equipment, many metallurgical problems arise which are of paramount importance in determining the performance of the equipment. The object of this article is to indicate briefly some of the important metallurgical problems.

Table I. Estimated Use of Copper in the United States, 1936–40, in Short Tons*

Use	1936	1937	1938	1939	1940
Electrical manufactures	164,000	212,000	150,000	185,000	247,000
Telephone and telegraph	26,000	40,000	30,000	39,000	49,000
Light and power lines	72,000	83,000	62,000	67,000	74,000
Wire cloth	6,500	6,800	6,000	8,000	9,200
Other rod and wire	90,000	102,000	60,000	95,000	120,000
Ammunition	11,900	14,100	12,500	14,500	26,000
Automobiles	108,000	112,000	55,000	85,000	103,000
Buildings	71,000	70,500	67,500	89,000	102,000
Castings	39,000	40,000	31,000	33,000	35,000
Clocks and watches	3,400	4,000	3,000	4,000	4,400
Copper-bearing steel	3,900	4,600	2,600	4,200	4,700
Radiators, heating	2,000	2,100	2,000	3,600	2,900
Radio receiving sets	24,000	23,100	21,000	27,000	32,000
Railway equipment	4,000	7,100	1,700	2,700	5,700
Refrigerators	15,000	13,500	6,700	10,000	10,500
Shipbuilding	5,000	6,400	6,000	8,500	8,700
Air conditioning	6,400	7,200	6,000	6,000	6,000
Other uses	65,300	66,600	46,200	67,600	81,500
Manufactures for exports	31,600	45,000	38,800	51,900	148,400
	749,000	860,000	608,000	801,000	1,070,000

* From Minerals Yearbook Review of 1940.

Pure copper is used extensively in electric equipment because of its high electric conductivity. The tensile strength of copper is low and its specific gravity is high, so that it is generally unsuitable for structural use. However, some of the alloys of copper exhibit exceptionally good mechanical properties (for example, beryllium copper) and are widely used for structural purposes.

The tensile strength of soft copper is about 31,000 pounds per square inch, and, before fracturing, it elongates approximately 40 per cent. Hard-drawn copper has about twice the tensile strength of soft copper, but the elongation is only a few per cent.

Copper used for electric conductors must be of high purity. Typical compositions of tough-pitch and oxygen-free high-conductivity copper are given in Table II. The total of the metallic impurities in both types of coppers is only about 0.01 per cent. The need for such high purity becomes obvious when Table III is studied. Small amounts of impurities may greatly reduce the electric conductivity of copper and thus limit the efficiency of electric equipment in which the material is used.

The conductivity of high-purity copper is affected strongly by cold working. For example, when the cross-sectional area is reduced 85 per cent by cold drawing, the conductivity drops from 102.3 to 100.0 per cent. Consequently, annealed copper is used whenever high conductivity is desired.

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E. R. Parker is with the research laboratory, General Electric Company, Schenectady, N. Y.

Table II. Typical Analyses of Oxygen-Free and Tough-Pitch Coppers Used for Electric Conductors

Elements	Oxygen-Free Copper	Tough-Pitch Copper
Silver	0.0022	0.0015
Iron	0.0015	0.0007
Lead	0.0009	0.0001
Sulphur	0.0023	0.0013
Arsenic	0.0004	0.0003
Nickel	0.0008	0.0002
Bismuth	0.0001	0.00001
Tellurium	0.0002	0.0001
Selenium	0.0003	
Manganese	0.003	
Tin	0.00005	Nil
Antimony	0.0009	0.0002
Oxygen	0.00000	0.035
Copper (balance)	99.988	99.961

Table III. Effect of Impurities on the Conductivity of Copper*

Per Cent of Element Present	Conductivity of Copper Containing							
	Cadmium	Silver	Tin†	Nickel	Tellurium	Antimony	Cobalt†	Iron†
0.001	102.3	102.3	102.1	102.2	102.1	102.1	101.8	101.5
0.01	102.3	102.2	101.4	101.3	101.0	100.6	98.1	95.0

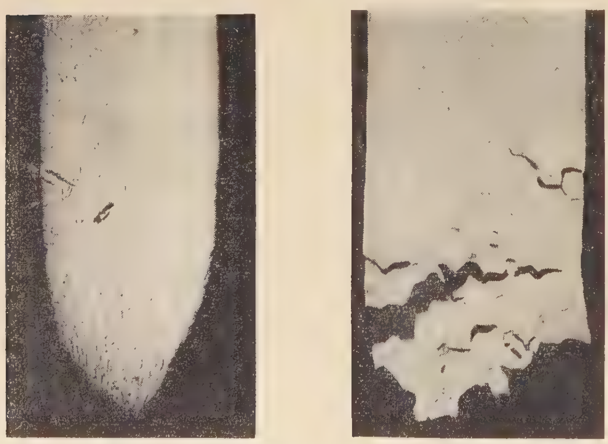
The conductivity of spectrographically pure copper is 102.3.

* From data by Smart, Smith, and Phillips (see references 1, 2, and 3).

† The tabulated data are for *oxygen-free* coppers; in oxygen-bearing coppers, tin, cobalt, and iron are present as oxides and, therefore, do not affect the conductivity.

Small amounts of impurities also have profound effects on the annealing characteristics of copper. This has long been known, but some recent quantitative data by Smart, Smith, and Phillips[1,2,3] are worthy of attention. They added various amounts of other elements to spectographically pure copper and studied the annealing characteristics of these alloys. Some of the results they obtained are summarized in Table IV.

A survey of the table will show the great effect of some impurities on the softening temperature. Particularly outstanding is the effect of tellurium. As little as



(*a*) (*b*)

Figure 1. Low-temperature transcrystalline fracture of a copper bar (a) and high-temperature intercrystalline fracture of a copper bar (b) magnified seven times

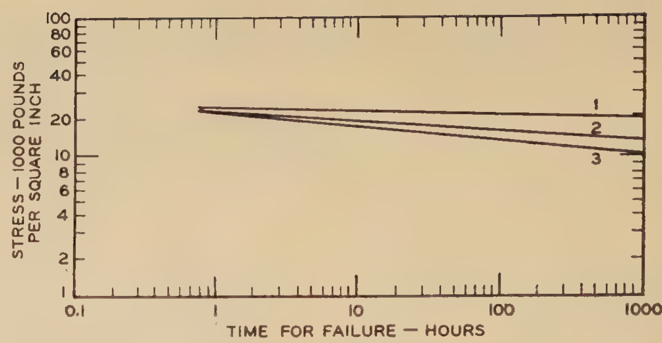


Figure 2. Curves of stress versus life for three types of copper tested at 200 degrees centigrade

1. *Tough-pitch copper containing silver*
2. *Tough-pitch copper*
3. *Pure copper*

0.001 per cent is sufficient to raise the softening temperature about 80 degrees centigrade.

In the manufacture of electric equipment, it is desirable to be able to adjust the various properties of copper so that the most efficient use can be made of the equipment of which the copper is part. Generally, high conductivity is essential for high efficiency, and the allowable quantity of impurities thus is automatically limited. Certain applications of copper require operating temperatures above those normally employed. Under the influence of stress at high temperatures, copper often breaks with very little elongation. The apparently "brittle" failure follows the grain boundaries of the copper instead of going across the grains as a normal ductile failure does. Figure 1 shows both a short-time ductile transcrystalline fracture and a low-ductility long-time intergranular fracture. The intergranular type of failure can occur in equipment operating at temperatures above 125 degrees centigrade whenever the copper is subjected to prolonged loading. At higher temperatures, such fractures occur with lower loads and in shorter times, often without warning or noticeable deformation. At times these intergranular failures have caused much

Table IV. Effect of Impurities on the Softening Temperature† of Copper*

Per Cent of Element Present	Softening Temperature in Degrees Centigrade of Copper Containing							
	Cadmium	Silver	Tin‡	Nickel	Tellurium	Antimony	Cobalt‡	Iron‡
0.001	160	142	150	140	220	160	142	145
0.01	310	205	315	145	370	320	160	155

The softening temperature of spectrographically pure copper is 140 degrees centigrade.

* From data by Smart, Smith, and Phillips (see references 1, 2, and 3).

† The softening temperature reported in the table is the temperature to which the copper, after being reduced cold 75 per cent in cross section, must be heated for one hour to reduce the tensile strength to a value half way between those of the hard-drawn and annealed conditions.

‡ The tabulated data are for *oxygen-free* coppers; in oxygen-bearing coppers, tin, cobalt, and iron are present as oxides and therefore do not affect the softening temperature.

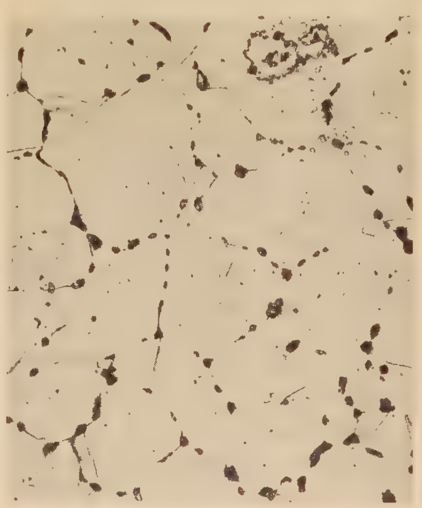
trouble in the manufacture and in the use of some electric equipment. Such failures can be eliminated by a judicious balance of the impurities present in the coppers.

Tests of the type plotted in Figure 2 are useful in evaluating the influence of various elements on the high-temperature strength. Bars of each type of copper were held under constant load at the testing temperature, and the time for failure was noted. A series of such tests at various loads yields the "rupture curves" plotted in Figure 2. As the stress is lowered the life becomes longer. However, the ductility decreases, and the type of failure changes from the transcrystalline type characteristic of short time and low temperatures, to the intergranular type characteristic of long time and high temperatures. High-purity copper is very weak at 200 degrees centigrade. Commercial tough-pitch copper, which contains about 0.01 per cent metallic impurities, is considerably stronger. The addition of 0.039 per cent silver to the tough-pitch copper greatly improves the high-temperature strength and practically eliminates the undesirable intergranular fractures. Small quantities of other elements such as cadmium and manganese[4] also greatly improve the high-temperature strength without lowering appreciably the conductivity. Study of the high-temperature properties of copper indicates that small amounts of certain elements greatly increase its strength at high temperatures. The maximum operating temperature is now limited mainly by the available insulating materials.

Under present emergency conditions it is often necessary to overload transmission lines. The question naturally arises, what is the maximum temperature at which the transmission lines can be operated without long-time high-temperature fractures occurring? The answer differs for each copper and each installation, but in specific cases the question is being answered successfully by means of tests similar to those recorded in Figure 2.

The foregoing discussion has been concerned mainly with the materials having good high-temperature strength, a factor which seems to be associated with high softening temperature. However, there are many applications where a high softening temperature is very undesirable. One such instance that is frequently encountered is in the annealing of hard-copper wire during the baking of the enamel in the enameling process. The wire is run continuously through the enamel and then directly into the baking oven. If the baking and softening can be done simultaneously, soft-copper wire, necessary for certain applications, can be produced more rapidly and cheaper than if a separate annealing operation were necessary. The baking temperature is limited by the enamel, which must not be "burned" in the operation. Small amounts of impurities, particularly tellurium, antimony, and cadmium, prevent copper from softening. Consequently, great care must be taken to remove these elements during the copper-refining process whenever the copper is to be used in the enameling process. If random batches of copper are used, it is sometimes necessary, before processing, to separate those coppers with high-softening temperatures. These are used, then, for other purposes.

(*a*)



(*b*)

Figure 3. Tough-pitch copper (a) and tough-pitch copper embrittled by heating in hydrogen (b) magnified 250 times

Another difficulty often encountered in the electrical industry is hydrogen embrittlement of tough-pitch copper.[5] Whenever copper containing copper oxide is heated in an atmosphere containing hydrogen, the hydrogen may diffuse into the copper, unite with the copper oxide and form water vapor which cannot escape. The water vapor expands and causes large holes to form, thus embrittling the copper. Embrittled copper, shown in Figure 3, is very weak and has extremely low ductility. Consequently, contact with hydrogen must be avoided in the fabrication of tough-pitch copper. Oxygen-free copper is used whenever the copper must be heated in contact with hydrogen.

An interesting problem that as yet has not been solved is that of the copper-oxide rectifier. The metallurgical requirements of copper for good rectifiers are not clearly understood. Some coppers are good; others for no apparent reason are bad.

In conclusion, it should be pointed out that most of the metallurgical problems connected with the use of copper

in electric equipment are clearly understood. However, new designs and uses of equipment undoubtedly will bring forth many new problems to be solved through metallurgical research.

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Practical Education in Wartime

PHILIP W. SWAIN

"One outstanding characteristic of this wartime training or education is its extreme practicality. Another is narrowness. In the postwar period we must preserve the practicality but not the narrowness, for peacetime students must be trained to meet situations that cannot be foreseen in a rapidly evolving civilization."

LEST my subject be misleading, may I state that this article is neither a survey nor a statistical presentation. It is not an attempt to list the public and private agencies specializing in wartime education, nor is it a classification of the various types of schools and courses. Most of this machinery is a passing phase. My concern here is to review, in the light of our latest wartime experience and with an eye to the future, certain personal views and convictions about the objects and methods of education. Let us reconsider the two basic questions:

1. What is worth learning?
2. What is the best way to learn it?

Not now a professional educator, I speak as a man who still makes a hobby of education. At various times I have done practical teaching—have tutored physics, taught power engineering and mechanics of materials, drilled soldiers in the last war, instructed artillery officers in ballistics and orientation, and more recently, have given a few university courses in business journalism. All through these years, when I haven't been teaching, myself, I have been watching other teachers, studying their results, and pondering the aims and methods of education. As a result, gradually I have acquired certain strong convictions, which I shall set forth for your consideration.

There can be no better time than now to discuss education, while the explosions of war are shaking all our institutions of learning and teaching. Everywhere the cement of educational tradition is cracking. Teachers, administrators, programs, and methods are forced to justify themselves by their works, rather than by their forebears. Right now, before we settle back into the old routine, is the time to study what is being done, and to get set for the great postwar educational advance.

My thoughts and comments mostly look ahead, because already we are finding out how to educate for *this* war, and those whose job that is are doing it fairly well in the colleges and technical institutes, in the vocational and public schools, in the training camps, at sea, and on the battlefields. One outstanding characteristic of this wartime training or education is intense practicality. Another is narrowness. In the postwar period we must preserve the practicality, but not the narrowness, for peacetime students must be trained to meet situations that cannot be foreseen in a rapidly evolving civilization.

From recognizing the need for breadth, some persons have "sailed through the air with the greatest of ease" to the unwarranted conclusion that the traditional liberal-arts course, long on the humanities and short on physical science and vocational subjects, is an excellent all-round preparation for life in the world ahead. As a Bachelor of Arts graduate, myself, I must register violent disagreement with this view.

CULTURE VERSUS VOCATION

Strangely enough, the battle between culture and vocation is still being waged, and on false premises, although the classicists have lost much ground since those distant days when every gentleman scholar was expected

Philip W. Swain is editor of *Power*, McGraw-Hill Publishing Company, New York, N. Y.

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to master both Latin and Greek—the days when no self-respecting university would prepare students for a vocation other than that of doctor, lawyer, parson, or gentleman of leisure.

Since the very meaning of culture is often debated, let me define it as enlightenment, discipline, and refinement, obtained by mental and moral training.

I believe in the maximum cultivation of the mind and spirit, but do not conclude, therefore, that the six years I devoted to the study of Latin and the five to Greek were a justifiable allocation of time and human energy—not even from the cultural angle. I speak with feeling, for I gained my machine-shop experience during the summers between terms of Greek study. I am positive that the shop experience had more purely cultural value to me than the Greek, to say nothing of its relative vocational practicality.

You must pardon me for appearing to shout at a man of straw. To study or not to study Greek is no longer an educational issue in America. That battle was lost decades ago. But the war of curricula—culture versus vocation—goes on. The overlooked truth is that all sound vocational training is, in itself, highly cultural. It is equally true, of course, that certain subjects with little vocational application have such great cultural value as to justify a big place in the curriculum, but let them hold this place because of something better than a certificate of vocational uselessness.

I would classify elementary physics, chemistry, mathemathics (mainly arithmetic), practical English (training in reading, writing, and speaking the language), practical economics, typewriting, and elementary drawing as being definitely of vocational value to most Americans on most jobs. Such other subjects as shopwork, biology, and advanced sciences and mathematics, have vocational value for specific groups. Let these subjects be selected first on the basis of their vocational usefulness to the student concerned. Each one will then do double duty, because each one is highly cultural, also.

Next, let's put into the curriculum those other subjects most essential for sound citizenship, such as geography, history, civics, and general economics. These, too, are also cultural, and thus again do educational double duty. Then, if there is time, one should add certain subjects that will give the greatest return in human satisfaction though essential neither to occupation nor good citizenship. I have in mind, particularly, English literature, modern languages, music, and art.

I think it will be conceded, generally, that the only proper object of education is to prepare the student to live a happy and useful life. That is why truly vocational training automatically must take first place as the prime essential for both usefulness and happiness. Let us never forget the joy of doing a job well. Let us never imagine that it is more cultural to live on relief than to draw an adequate pay check, or that those who do not know how to provide food and clothes for themselves ever will have much opportunity to live the higher life.

As to subject matter, my fundamental conviction is that primarily, education should be vocational; secondarily, a training for citizenship; and thirdly, purely cultural to the extent of the remaining available time and facilities; and that all three of these are cultural in the broad sense of the word. In round figures, sound education is 80 per cent practical and 100 per cent cultural.

After subject matter comes the great question of method. Examples of good teaching and good learning are to be found here and there, but inept teaching and bad study habits are more common on all levels of education from kindergarten to university graduate courses. Consider, for example, the so-called "lecture courses" that long have been an educational scandal, particularly in liberal-arts colleges. I am referring to courses in which the professor stands before and lectures to a large group of students three or four times a week with no student participation other than listening and note taking—no laboratory work, and no conferences. The professor stands up there and talks, and the students, like sponges under a dripping faucet, are supposed to swell up gradually with the moisture of knowledge.

"Lecture courses" may be justified in the case of such intangible subject matter as philosophy, ethics, human relations, and politics, where some great and mellow personality can broadcast atmosphere, attitude, and human lore from his lecture platform. To attempt to teach physics, chemistry, mathematics, or any precise discipline or practical skill *primarily* by lectures is nothing short of educational asininity, in my opinion. One cannot master such subjects by listening to someone else talk. In these fields one can never acquire an education except by rolling up his own sleeves, burning his own midnight oil, solving his own problems, sweating over his own laboratory project, asking questions, and grilling and being grilled by a smart teacher in the give-and-take of conference and recitation.

Good teaching keeps the learner *doing* something. Education not accompanied by muscular action doesn't stick, and, therefore, is a shoddy product. I believe in drill, drill, and more drill; things heard and tried but once are soon forgotten. Superficiality is the bane of much modern education, particularly in those high schools and colleges that try to "cover a lot of ground." We need far more thoroughness in education.

Note that I did not say "completeness." Many high schools today are so crowded with required courses that the dizzy students get nothing thoroughly except mental indigestion. If the number of courses cannot be cut, then the content of individual courses should be reduced.

Let's shorten the history book and really learn what's left. Let's drop some of the secondary information at the end of the trigonometry book to make certain the

student is amply drilled in what is really important—the use of sines, tangents, and cosines in measuring and laying out angles. The same suggestion applies to physics, chemistry, and English literature. In each field the specialists are trying to cram so much detail into young heads that nothing is retained.

I have expressed a low opinion of any attempt to teach precise scientific and vocational disciplines by courses devoted predominantly to lectures. To the extent that one cannot learn by lectures, how can one learn? In my opinion one can learn best by reading and by doing.

First, let's consider books. Wherein is the reading of a book any better than listening to a lecture? It won't be if you don't know how to read. The worst thing about a lecture is that the talker plunges ahead without knowing whether you, the listener, are dragging behind or running a mile ahead. If one reads a textbook straight through without stopping for breath, one might as well be listening to a lecture. But it isn't necessary to study that way. The man who knows how to study any exact scientific and logical discipline out of a book pauses every few minutes to make a sketch, spot a location on the map, look up a word in the dictionary, or solve a numerical problem. He does something; he uses many muscles, and he fences mentally with the author. This activity seems to be a sort of waterproof adhesive that cements, that attaches the learning to the man so that it will not wash off with the first rain.

On the whole, it seems that the writers of educational books are a more serviceable tribe than the givers of lecture courses, but they are not above criticism, either—certainly not those who write long textbooks where short ones would serve better, who use big words and obscure expressions where a little more toil or humility would find short words to do a better job.

To some extent an educational book may be viewed as an automatic machine for the mass production of certain ideas and skills among large groups of people. Yet the book can never be a complete instrument of education, for it cannot spare the student the need to work his own problems, do his own thinking, and drill himself for practical mastery of its contents.

Because the book is a product of, as well as an instrument of, mass production, it is the least costly of all educational tools per unit of work accomplished. The man who really masters a carefully selected, three-dollar technical book will likely devote $100 worth of time and effort to the job of mastering it, and thereby will acquire skill with a market value of a thousand dollars. In this matter of books, a little side remark may be in order. After many discouraging experiences with long books, I have acquired almost a mania for short ones.

All of us, when we leave school, plan "some day" to continue or to take up the study of literature, history, astronomy, differential equations, or what have you. Most of us never will—not to our dying day. Why is it? I think I know. As college graduates, we feel that we should tackle the selected subject in an ample scholarly volume. It's a great mistake. Unless you are a very unusual person, take my advice; find yourself a little book, a primer—probably something written for seventh-grade students, or immigrants, or maybe even morons. Incidentally such books often are written with more care and skill than those directed at more advanced students. Study your little book from the first page to the last, and master everything in it. Almost invariably you will discover something you never knew before, enough to pay you for your time and investment. Then, if you still have any ambition left, go through the subject again with a larger book—again, thoroughly. That formula, I am certain, is the only cure for the average graduate's failure to continue profitable home study once he gets beyond range of the schoolmaster's lash.

I believe in lectures only slightly. I believe in books a lot more. Yet, I would consider any educational system defective in which the student spent as much as 50 per cent of his learning time in lectures and book reading combined. This means that he should spend more than 50 per cent of his time in solving problems, drawing pictures, working in shops and laboratories—doing almost anything except reading or listening to lectures.

This question of doing, as opposed to listening and reading, leads directly to the question of practical shop experience for engineers. Certain of the engineering schools have no student shops. Others are reducing the time spent in college shopwork. Some educators are saying that college shopwork doesn't fill the bill. Perhaps they are right; I can't argue the point because all my own shop experiences was obtained in commercial machine shops in summer vacations.

Where the shop experience can best be had is a proper subject for discussion, but on one point there can be no disagreement—the engineer who graduates from engineering school without a substantial shop background obtained somewhere and somehow definitely is handicapped. Creative design and engineering management rest as much on trained intuitions as on academic knowledge. Only work with one's own hands in an actual shop can give one the right "feel" of metals, machines, working people, and working methods.

Some may ask whether this experience cannot be delayed until after graduation. I say no. Why deny the student the early practical background that will help him evaluate his college studies? Why inflict on the young graduate the mental sufferings of a degree-labeled complete tenderfoot in industry? Why make things harder for the young graduate by setting up the degree as a social barrier before he has had a chance to know workmen from their own level? Why ask an older man to learn slowly what he could have learned better when young?

So far I have expounded my personal views. Let's

consider what others say. I asked a number of leading engineering educators, "Is this war teaching us something about the art of learning and the art of teaching—something we can apply after the war, to the end that our people may live more happily and more effectively?" The complete answers are extremely interesting, but space permits only a brief summary of the high spots.

One who asks to be identified only as an unconventional, iconoclastical, metropolitan dean, says that many engineering colleges have been brought in touch for the first time with young men in engineering positions in industry through the engineering, science, and management war training (ESMWT) courses. In industrial background and sincerity and appetite for knowledge these night students from industry far outshine their day brothers, says this dean. He foresees a great postwar extension of night degree courses and an elimination of the traditional snobbish faculty attitude toward such courses.

In Rochester, N. Y., is one of America's most successful institutions of practical education—the 114-year-old Rochester Athenaeum and Mechanics Institute, with a present enrollment of 4,500. President Mark Ellington of the Institute writes that traditional education involves too much lecturing and book work and not enough doing. The reasons, he says, are custom plus the higher cost of doing the job right. Because of the war, millions of men in and out of the armed services have been given practical, intensive instruction. Never again will they be satisfied with traditional procedures.

From Washington writes M. J. Kane, assistant director, training within industry, War Manpower Commission: "There is no need to seek further for new ideas in connection with training. The imperative thing is to get action and to do some of the things which we have known for many years, but have only talked about. We now find that the need is for specific direct help to individuals to meet their particular problems. Such things as job instruction, job planning, and job-relation training have been reduced successfully to short ten-hour units. When given to hundreds of thousands of supervisors, these will affect favorably the entire fabric of all industries."

Dean A. A. Potter of Purdue University, Lafayette, Ind., who has contributed greatly to the adjustment of engineering schools to the country's wartime needs, says it is certain that our educational institutions of higher learning will not be taken over by government. From wartime activities we are learning to compress information in undergraduate curricula. For the future he expects less specialization in the undergraduate curriculum, but increased emphasis on new types of courses, particularly in the field of electronics. Dean Potter foresees increasing importance of the technical-institute type of education, giving practical education without degrees.

Professor Theodore Baumeister, head of the mechanical engineering department at Columbia University, New York, N. Y., asks: "Has not the experience of the last few years demonstrated that the previously prevalent and traditional forms of education are seriously wanting?" In particular, he questions the value of the Bachelor of Arts degree obtained with no study of true science, technology, physics, chemistry, biology, and similar courses. Looking to the future, he suggests that the heavier requirements on engineers may necessitate more graduate work.

R. L. Goetzenberger, vice-president of Minneapolis-Honeywell Regulator Company believes that universities and local industries will be forced to co-operate in the training of efficient industrial managers. To insure success of this work he recommends that the colleges virtually draft capable industrial administrators in the vicinity to serve as part-time consultants and practical lecturers.

SUMMARY

In conclusion, let me sum up my present personal views on education:

1. The classical tradition, long on the defensive, will continue to lose ground.

2. Vocational competency and good citizenship are the noblest aims of education.

3. Some of the highest cultural values are a natural by-product of sound vocational education.

4. Certain nonvocational studies that pay high dividends in human satisfaction (music, art, literature, modern languages, and the like) will be accepted eagerly everywhere, whereas those humanities that cannot stand on their own feet will "get the gate."

5. The explosions of global war will shake American educational institutions to their foundations, and will force revaluation all along the line.

6. The quality of teaching and learning must improve, with more attention to thoroughness and less to extent of coverage.

7. "Lecture-course" methods of teaching exact sciences and practical skills should be discredited as sloppy, superficial, unworthy of respect.

8. This does not imply that lecture courses are unsuitable for the teaching of such intangibles as ethics, philosophy, and politics.

9. Lectures also have a definite place in the teaching of the rigorous scientific and practical disciplines, but only when paired with far more time devoted to work and expression by the student.

10. There will be growing appreciation of the school book, or home-study book that is concise, simple, and practical.

11. More and more adults will study this or that subject for self-advancement or pleasure, using skillfully prepared short books and skillfully taught, short, nondegree courses in a great variety of institutions.

12. It will become more obvious that an education obtained by passive absorption, without auxiliary use of the muscles, is rarely worthy of the name. He who would study with profit must talk, write, draw, or handle tools—somehow must constantly put into action what he learns. Otherwise, he won't learn.

13. The power of the right education to create wealth, happiness, and domestic and international tranquillity is almost beyond belief. Nothing in the world is more important than doing this job right. Let's get to it!

Synthetic Rubber in the Making

Figure 1. These 30,000-gallon storage tanks contain basic butadiene and styrene

Butadiene, a gas composed of hydrogen and carbon, and styrene, a liquid derivative of coal tar and petroleum, are the principal constituents of buna-S synthetic rubber. The Institute (W. Va.) Plant, operated for the government by the United States Rubber Company, has nine of these tanks for butadiene and three for styrene. This storage capacity is sufficient to operate the plant for three days, and will produce enough rubber to make 190,000 passenger-car tires

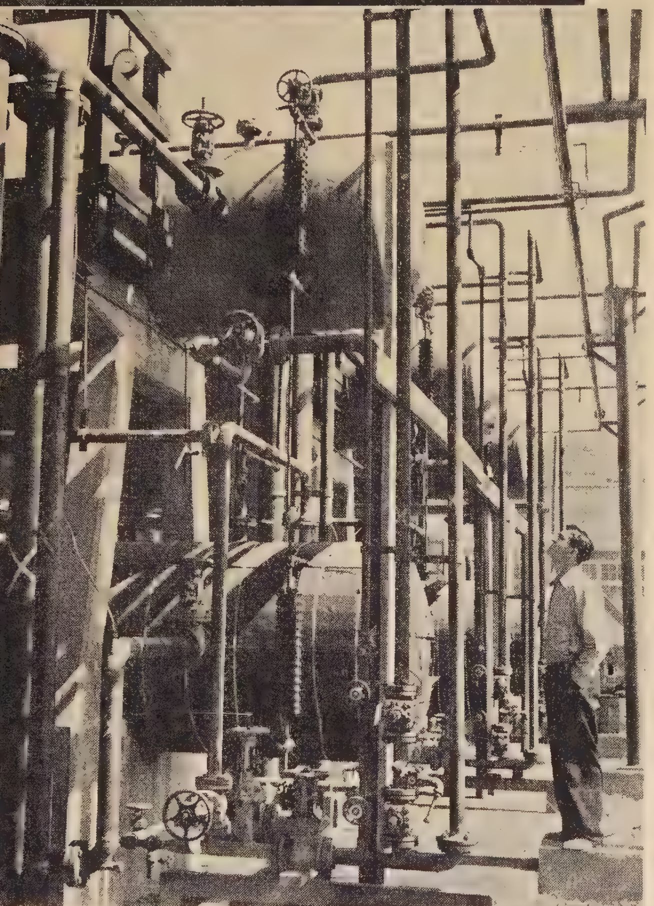
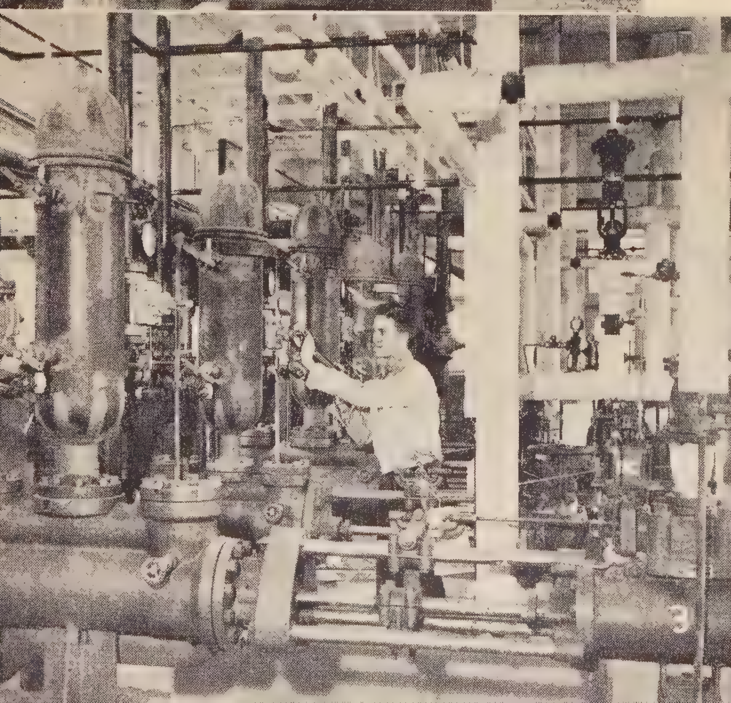


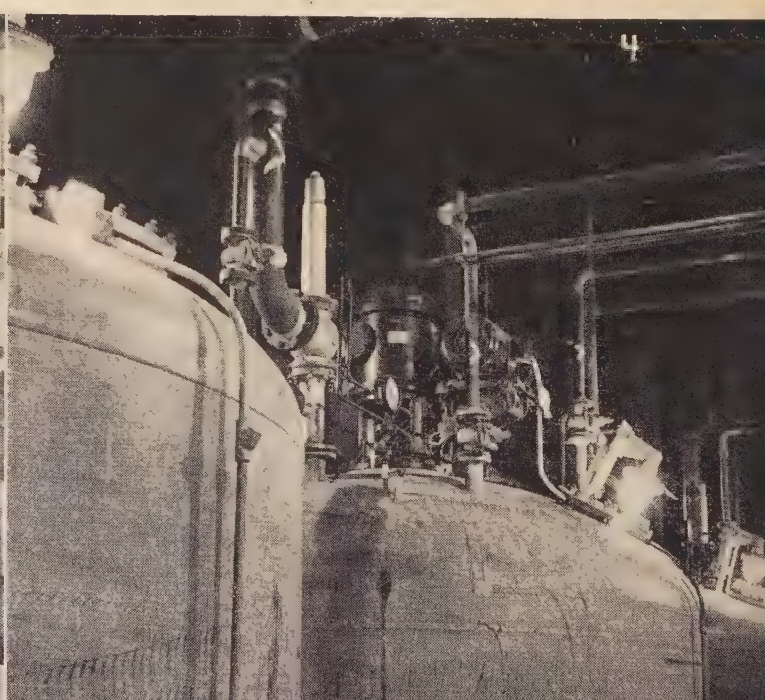
Figure 2. Apparatus used to remove the chemical inhibitor which must be added to butadiene in storage to prevent premature polymerization

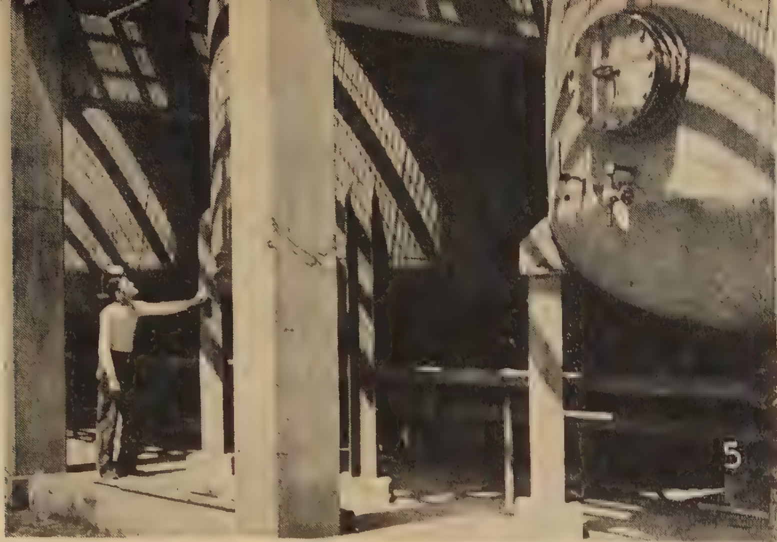
Figure 3. Transfer pump house, where the pumps which transfer the raw materials from the storage tanks to reactor areas are located

Three parts of butadiene to one part of styrene, together with special chemicals, are moved through pumps and pipe lines (Figure 3) to large glass-lined reactor vessels (Figure 4), where the mixture is agitated and polymerization takes place

Figure 4. Top view of some of the plant's 72 reactors, which can produce 90,000 long tons of synthetic rubber annually







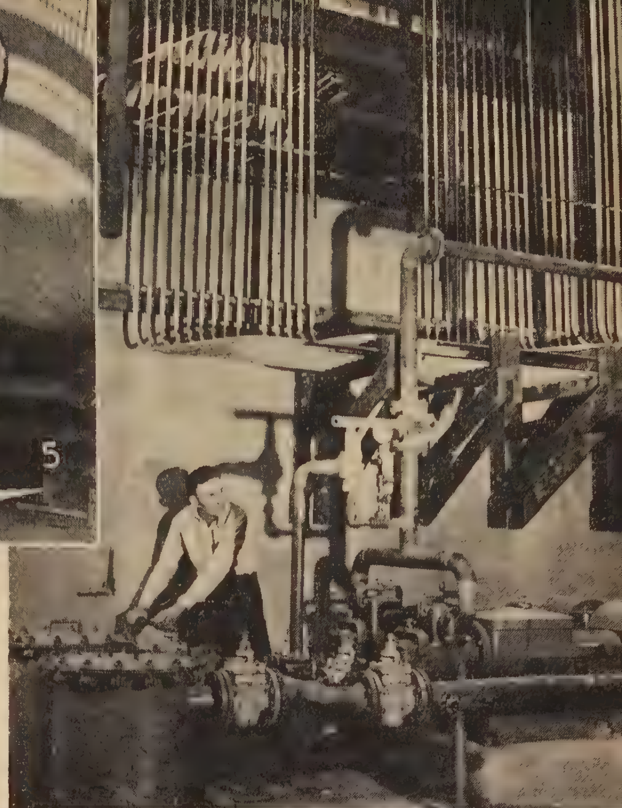


Figure 5. Blowdown tanks, into which the polymerized mixture is blown down to arrest the reaction

Unreacted butadiene and styrene are recovered here by vacuum and steam for return to the process. At this stage the mixture forms a basic latex similar to natural rubber

Figure 6. The synthetic rubber latex is batched and blended in the plant's 12 30,000-gallon storage tanks

Blending of the latex assures uniformity of the product. The rubber content of the latex is then coagulated by acid and separated out in the form of flocs or crumbs

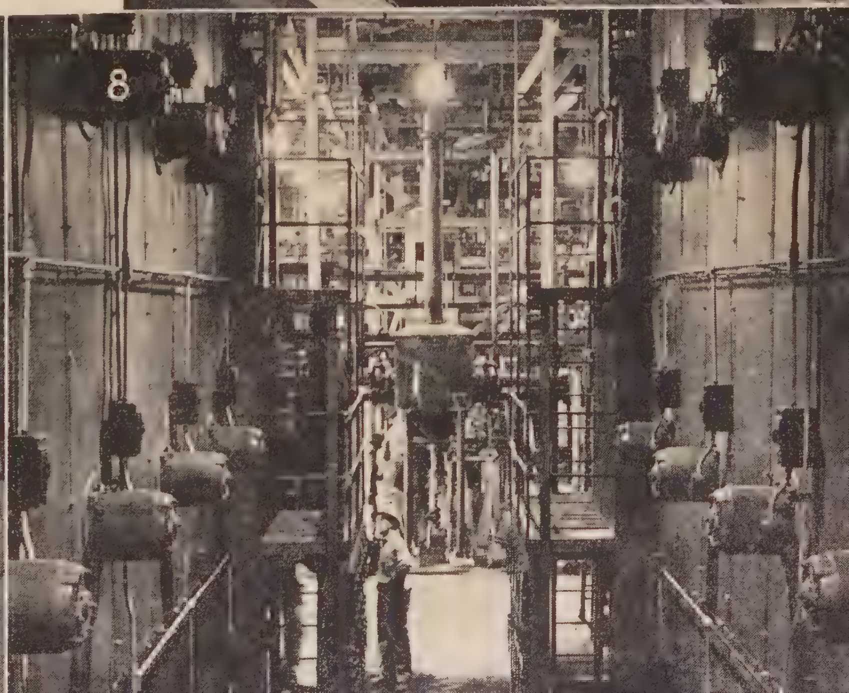
Figure 7. Flocs of synthetic rubber being washed to remove the excess chemicals used in previous stages

Figure 8. Steam driers, which remove all water from the soggy rubber crumbs

The crumbs of rubber are passed back and forth three times on a slowly moving belt of metal through these driers. At full operation, 12 of these driers will evaporate 160 tons of water a day

Figure 9. Loaf of baled rubber—the final stage in the manufacturing process—ready for production purposes

Bales are packed into treated cartons to prevent sticking; 9,000 such loaves are produced daily at this plant



INSTITUTE ACTIVITIES

Wartime Power-System Problems Feature Roanoke Technical Sessions

In accordance with the wartime policies adopted last year by the AIEE board of directors, heavy emphasis was placed on problems related to the war at the AIEE Southern District technical meeting held November 16–18, 1943, at Roanoke, Va. In fact, almost every single item on the program was related either directly or indirectly to problems imposed by the war.

As previously scheduled, the technical program comprised four sessions at which 13 papers were presented. Wartime operating problems of the power companies in the Southern District territory predominated at these sessions. An opening session, two evening sessions (one of which was a dinner meeting), and a student conference rounded out the program. In addition, the executive committee of the Southern District held a luncheon meeting on the opening day.

Total registration at the three-day affair was 220, which was especially good, considering that the 147 members of the host Section are distributed throughout the state of Virginia, and in the city of Roanoke itself there are only six members. Attendance at all sessions was excellent.

The Southern District of AIEE is comprised of 11 Sections and 18 Student Branches in the states of Florida, Alabama, Mississippi, Louisiana, Georgia, Kentucky, Tennessee, South Carolina, North Carolina, and Virginia. This was the first District meeting to be held in the state of Virginia.

President Funk Spoke at Opening Session

The three-day meeting was officially opened by General Chairman A. P. Gompf (A '27) Chesapeake and Potomac Telephone Company of Virginia, Richmond, who presided at the opening session. Mayor Leo F. Henebry of the city of Roanoke extended the official welcome to those attending the meeting, declaring that the electrical industry contributes more to our daily lives than any other industry. AIEE Vice-President C. W. Ricker of New Orleans, representing the Southern District, responded and then spoke briefly on certain aspects of the meeting.

Principal address of the session was delivered by AIEE President Nevin E. Funk. He predicted a new era in electricity after the war and declared that we have as yet only scratched the surface of absolute scientific knowledge.

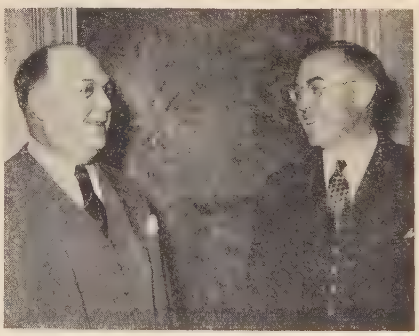
In response to those who advocated discontinuing national and District AIEE meetings during the war in order to save transportation, Doctor Funk said that these meetings are needed now more than ever. The majority of Institute members are now in new activities and therefore need the exchange of ideas which these meetings make possible. He also reported that a joint intersociety committee has been organized to study postwar problems.

Doctor Funk lamented the fact that the engineering profession as a whole has no national voice as have the professions of law and medicine. A joint intersociety committee is now studying this problem. Joint meetings which are becoming increasingly popular among local societies and local organizations of national societies represent an important step forward in the direction of greater solidarity within the profession, he concluded.

Technical Sessions

Wartime operating problems of power companies in the southeastern states and their solution formed the general subject matter of the four technical sessions of the recent AIEE Southern District meeting in Roanoke, Va. Specific subjects discussed included: the application of capacitors; design and operation of governors and tie-line control; new generating equipment recently installed to handle wartime increases in load. Other subjects included boatbuilding, gas-filled cable, new materials now being developed, and the technical training programs of the United States Armed Forces. Attendance at all sessions was exceptionally good.

Presiding at the sessions were: Herman B. Wolf (M '37) Duke Power Company, Charlotte, N. C.; Stanley Warth (M '36) Southern Bell Telephone and Telegraph Company, Jacksonville, Fla.; J. Elmer Housley (M '39) Aluminum Company of America, Alcoa, Tenn.; and H. E. Wilson (M '32) Carolina Power and Light Company, Raleigh, N. C. [in place of A. S. Hoefflin (M '40) Louisville Gas and Electric Company, Louisville, Ky., who was unable to attend].



Attending the Roanoke meeting were N. E. Funk, president of AIEE, who spoke at the opening session and E. W. O'Brien, vice-chairman of the committee on Student Branches, who presided at one of the evening sessions

APPLICATION OF CAPACITORS

Three papers on capacitor applications comprised the scheduled presentations at one session. Use of these units to improve voltage regulation of heavily loaded circuits, to supply kilovars at the load and thus avoid or postpone the necessity of installing new generating equipment, and to meet special conditions was discussed.

A study of the application of capacitors on a system-wide basis, made by means of the network analyzer, was reported in a paper, "The Effect of Kilovar Supply on the Design of Systems for Load Growth," by T. W. Schroeder (A '37), J. W. Butler (M '38), and N. H. Meyers (A '41), of the General Electric Company, Schenectady, N. Y. Mr. Schroeder presented the paper. Even though a conservative system was chosen for analysis, the results showed that the supply of kilovars by capacitors installed at or near the load is advantageous, both economically and otherwise. This paper has been approved for re-presentation at the AIEE 1944 winter technical meeting and is scheduled for inclusion in the *Transactions* section of the February 1944 *Electrical Engineering*.

A 13,500-kva bank of static capacitors installed at the Newport News substation of the Virginia Public Service Company was described in a paper by V. R. Parrack (M '31) of that company and E. L. Harder (M '41) of the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. The paper was presented by Mr. Parrack. Wartime increases in load in the Newport News area were such that, although existing generating and transmission facilities could handle the kilowatt requirements, they could not carry both the required kilowatts and kilovars. Therefore, it was decided to supply the kilovars at the substation. Advantages of faster delivery, use of less critical materials, lower cost, greater flexibility, and lower losses led to the selection of capacitors rather than a synchronous condenser. Also the capacitors had the added advantage of not increasing the interrupting duty of the station circuit breakers. The installation comprises 900 15-kva pole-type capacitor units arranged in five sections and switched in blocks. Operating experience was reported to be quite satisfactory.

Capacitor applications in pumping stations of the Big-Inch and Plantation oil pipe lines were described by M. A. Hyde, Jr. (A '27) of the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. Although the substations along these lines are very similar, their power

Among those at the Roanoke meeting were: C. W. Ricker, vice-president, Southern District; A. P. Gompf, chairman, general meeting committee; E. S. Fitz, vice-chairman, general meeting committee; F. E. Johnson, Jr., secretary, Southern District; and T. H. Mawson and F. H. Sittloh of Birmingham, Ala.

services vary widely. As a result, capacitors were installed at several substations, principally to improve the running conditions of the pump motors. Special conditions at one substation, however, made it desirable to install two capacitor groups, one for continuous duty and one for intermittent duty during motor-starting periods.

GOVERNORS AND TIE-LINE CONTROL

The close interrelationship between prime-mover speed governors and tie-line control equipment was emphasized in three papers presented at one session, two on governors and one on tie-line control.

C. L. Avery, Woodward Governor Company, Rockford, Ill., outlined the design and operating characteristics of modern hydraulic governors for hydroelectric units and pointed out that the governing problem is a phase of the larger problem of system stability. Modern governing systems have an over-all sensitivity of $^1/_{20}$ of one per cent and a speed regulation capable of adjustment from zero to five per cent, as required. Mr. Avery also described a special type of frequency recorder developed to check the performance of governors, presented test results demonstrating its usefulness, and discussed performance of governors when operating in conjunction with automatic frequency and load control.

A second paper on hydroelectric-unit governors, presented by J. F. Roberts, of the Allis-Chalmers Manufacturing Company, Milwaukee, Wis., outlined the seven major functions of governors, only two of which require extreme accuracy or sensitivity: instantaneous frequency control and tie-line loading. Modern governors, Mr. Roberts said, show a sensitivity of from $^1/_{25}$ to $^1/_{100}$ of one per cent. Proper care and maintenance are required to retain this sensitivity. Mr. Roberts also drew some comparisons between the problems of governing steam and hydroelectric units.

Design and application of tie-line load-control equipment were discussed by S. B. Morehouse (A '35) Leeds and Northrup Company, Philadelphia, Pa. He stated that the co-ordination of tie-line control equipment with governors is necessary for best tie-line operation. The tie-line control causes the governors to communicate the varying requirements of the load to the generators. Tie-line control equipment is co-ordinating load distribution throughout load areas, Mr. Morehouse said in outlining the application and operation practices in large interconnected systems. Simplification is desirable within limits, but the equipment must be flexible enough to meet the requirements.

Discussion of these papers revealed that the point of view of the power-system operators differs somewhat from that of the equipment manufacturers. Although there appeared to be general agreement as to the need for proper co-ordination of tie-line control and the governors, there was some difference of opinion as to the relative importance of the governors. It was pointed out that there must be proper co-ordination between the tie-line control and the system relays. The belief was expressed that, since frequency and load control are electrical problems, the governing might also be appropriately accomplished electrically rather than mechanically as at present. One operator stated that most of the governors on a system do not contribute to frequency control, but serve mainly as "watch dogs" to function in case the generating units become isolated from the rest of the system. Another operator took the opposite view that modern governors make possible the present widespread interconnected operation. Opinion seemed to be unanimous as to the need for careful maintenance of equipment.

NEW GENERATING EQUIPMENT

Two new generating stations and new equipment at two other stations were described in three papers. C. C. Dodge (A '20) of the Stone and Webster Engineering Corporation, Boston, Mass., discussed the new Chesterfield station near Richmond, Va., now under construction. The keynote of design of this station was the conservation of critical materials, featuring such wartime practices as the use of wood in place of steel in many instances and the use of corrugated asbestos board for outside wall coverings. In spite of many departures from conventional practice, the designers believe it will prove a reliable source of power. The station will house one 50,000-kw unit supplied from a single coal-fired boiler capable of furnishing 525,000 pounds of steam per hour at a pressure of 875 pounds per square inch and 900 degrees Fahrenheit.

The other new station featured was the Paddy's Run station of the Louisville (Ky.) Gas and Electric Company, described by M. S. Winstandley (A '29) of that company. Unlike the Chesterfield station, this station was designed and the materials purchased for it before the war. The design therefore is conventional. Although intended for an ultimate capacity of 300,000 kw, it now contains only two 25,000-kw 3,600-rpm hydrogen-cooled units. Steam conditions are 650 pounds per square inch and 900 degrees Fahrenheit. Generation began in July 1942. Its output is being absorbed largely by nearby war industries.

Wartime additions now under construction in two stations of the Virginia Public Service Company were described by G. M. Tatum (A '40) of that company, in a paper prepared jointly by himself and R. H. Boas of Gilbert Associates, Inc. A new 15,000-kw steam unit to operate at 625 pounds per square inch and 850 degrees Fahrenheit is being installed in the company's Alexandria station. At the Hampton station, a 6,000-kw topping unit to operate at 900 pounds per square inch and 870 degrees Fahrenheit is being added; it will exhaust into the station's present low-pressure system at 210 pounds per square inch.

In the discussion period following these three papers, Philip Sporn (F '30) American Gas and Electric Corporation, New York, N. Y., described wartime additions that have been made to generating and transmission facilities of the American Gas and Electric Company central interconnected system. The aggregate wartime increase in system capacity is greater than that of any other privately owned company in the United States, Mr. Sporn declared.

OTHER SUBJECTS DISCUSSED

"Influence of New Materials on Design" was the subject of a talk by R. C. Bergvall

(M '41) Westinghouse Electric and Manufacturing Company, Pittsburgh, Pa. New materials and methods have given us greatly improved equipments of many types, although the basic principles may have remained the same, he said. Electrical manufacturers use a wider range of raw materials than most other manufacturers; and, with the war restricting the supply of many of these materials, the industry has been conducting an intensive search for new materials and new methods of using materials. Mr. Bergvall described some of the advances already made in this direction and exhibited samples. He related how research in grain orientation in core iron has resulted in smaller transformers. He described some of the uses of the mass spectrometer and predicted new uses for this device. Speaking of electronic applications in industry, Mr. Bergvall discussed the expanding use of induction and dielectric heating at high frequencies and predicted that new more rugged tubes will extend the use of these devices in industry. High-temperature alloys that can be used in developing gas turbines are being formulated. He expressed the opinion, however, that small gas turbines for stand-by use are likely to be developed first, these units having relatively short life because of the high temperatures involved.

Wartime technical training was reviewed by W. S. Rodman (F '28) University of Virginia, Charlottesville, who traced the effects of the war on technical training from pre-Pearl-Harbor days to the present and discussed some of the difficulties faced by educators and students in the latest Army and Navy programs. For the latest Army specialized training program (ASTP) and the Navy college training program (NCTP), aimed to give technical training to men of officer caliber, some 30,000 candidates have been obtained as a result of competitive examinations among high-school graduates and 5,000 candidates from men already in the services. Many more candidates are needed. Refresher courses have been organized for those who have been away from school an appreciable time. Dean Rodman said that the courses are so intensive in nature that they are extremely difficult for the institutions to organize and teach; furthermore, even exceptional students can maintain satisfactory records only with the greatest difficulty. He expressed the hope that the program would be altered to alleviate some of these problems. Dean Rodman mentioned also, that many women are being given special courses to fit them for specialized duties in industry.

The construction and operating characteristics of a 38-kv low-pressure gas-filled cable for the underground section of a new feeder of the Virginia Electric and Power Company were described in a paper by W. A. Del Mar (F '20) Phelps Dodge Copper Products Corporation, Yonkers, N. Y., and A. F. Gambitta (A '26) Phelps Dodge Copper Products Corporation, New York, N. Y. This cable was selected after much careful thought to meet the fast expanding wartime load in the Hampton Roads area. The cable contains three 600,000-circular-mil sector-type impregnated-paper-insulated conductors and three 0.375-inch copper tubes for distributing the gas. Each insulated conductor is wound with a perforated copper shielding tape. Diameter over the lead sheath is 3.25 inches. One of the copper tubes is solid and continuous throughout the entire length of the cable, being tapped to the cable interior at the joints. The other two tubes are helical and distribute the gas between joints. Gas pressure within the cable is maintained between 10 and 15 pounds per square inch. A special relay sounds an alarm if pressure becomes too high or too low. After the paper was presented, W. F. Nimmo (M '35) of the power company, showed a motion picture depicting some of the more interesting installation steps.

War flavor of a somewhat different type was injected into the program by Milton L. Levy of the Higgins Industries, Inc., New Orleans, La., who described the construction of small naval vessels of specialized types, such as motor-torpedo boats and various types of craft for landing men and equipment. His talk was supplemented by motion pictures showing the testing of these craft.

Evening Sessions

Spokesmen of both the Army and the Navy were on the program of the dinner meeting Tuesday evening, November 16. J. H. Berry (M '31) Virginia Electric and Power Company, Norfolk, Va., presided.

Lieutenant Colonel M. P. Chadwick of the Signal Corps related some of his experiences during the Japanese sneak attack on Pearl Harbor and later, on Guadalcanal Island. He explained how radio and wire communication was used in the latter campaign. Colonel Chadwick reported that the equipment functioned well, but some trouble was experienced with wire lines. All component parts of radio must be waterproofed for use in jungle warfare, he said, and the units must be rugged enough for transport by jeep over rough terrain. Gasoline-engine-driven generators furnish the required electric power. Japanese radio equipment captured in that engagement had been manufactured in 1937–38.

Captain H. G. Rickover of the Bureau of Ships described some of the latest electrical applications on modern warships of the United States Navy and related how the radically different conditions encountered in the present war, as compared with the last war, have brought about many changes in practices and in equipment design. His talk was supplemented by slides showing electrical equipment that had been submerged in ships damaged at Pearl Harbor.

Feature address at the second evening session held Wednesday, November 17, was by E. H. Alexander of the General Electric Company, Schenectady, N. Y., who spoke on "Electronics in Industry." He said that electronics was being misrepresented to the general public in popular magazine articles and advertisements. Pointing out that industrial electronic devices have been in successful use for more than 20 years, he declared that we are not on the verge of an electronic age—we are already in it. Mr. Alexander then proceeded to discuss four different types of equipment, for (1) measurement, (2) power conversion, (3) heating, and (4) control purposes. His talk was supplemented with slides showing typical equipment.

Following the feature address, two motion pictures were shown depicting, respectively, in popular language, the inner workings of frequency-modulation (*FM*) radio and television. Eugene W. O'Brien (M '37) managing director and editor, *Southern Power and Industry*, Atlanta, Ga., presided.

Student Program

The main event of the Student Branch conference Wednesday at 2:00 p.m. was the presentation of three technical papers on a competitive basis by Enrolled Students. The papers presented were:

1. X Rays; **H. L. Fanning** and **H. Williams**, University of South Carolina.
2. Electronics and Torque Measurements; **R. E. LeBlanc III** and **E. G. Holmes**, Tulane University
3. Electronic Development and Applications; **B. C. Carr, Jr.**, Virginia Polytechnic Institute.

The prize was awarded to LeBlanc and Holmes.

E. W. O'Brien (M '37) vice-chairman of AIEE national committee on Student Branches, H. H. Henline, national secretary, and C. W. Ricker, District Vice-President, addressed the meeting informally. W. O. Leffell (A '37) counselor of the University of Tennessee Branch, Knoxville, was elected new District chairman of student activities and District counselor delegate to the 1944 annual meeting. It was decided to hold the next student conference at the University of Tennessee in April 1945. Claudius Lee (M '13) professor of electrical engineering, Virginia Polytechnic Institute, presided.

District Executive Meeting

The Southern District executive committee held its regular annual business session Tuesday, November 16, in Roanoke, Va. Attendance which totaled 24 members showed representatives from 10 of the 11 Sections in the District. Among those present were District Vice-President C. W. Ricker, District Secretary F. E. Johnson, Jr. AIEE President Nevin E. Funk, National Secretary H. H. Henline, and Past Vice-Presidents Mark Eldredge, J. Elmer Housley, and W. S. Rodman were present as guests.

J. Elmer Housley was elected representative to serve on the national nominating

Analysis of Registration at Roanoke

Classification	Virginia Section	District 4*	Other Districts	Total
Members	40	60	44	144
Enrolled Students	4	3	1	8
Men guests	34	10	14	58
Women guests	4	2	4	10
Totals	82	75	63	220

* Outside Virginia Section.

committee, and J. R. Smith was chosen alternate. Definite plans were made to hold the next Southern District meeting in the fall of 1945, probably in South Carolina.

A definite trend toward greater co-operation among the different branches of engineering through joint meetings in the field was reported. Most Sections are either sponsoring such meetings in co-operation with local engineering clubs or societies, or jointly with local groups of other national societies.

Committees

General committees and subcommittees which planned the meeting were:

General Committee: A. P. Gompf, *chairman;* E. S. Fitz, *vice-chairman;* C. W. Ricker, *AIEE vice-president representing the Southern District;* F. E. Johnson, Jr., *secretary,* Southern District; F. W. Chapman, E. R. Coulbourn, C. E. Creecy, R. M. Ferrill, J. F. Fossick, R. C. Fuller, C. P. Knost, J. E. Mellett, J. J. Strickland, C. F. Titus, and L. G. Weiser.

Hotels and Transportation: W. I. Whitefield, *chairman;* A. R. Hines and J. L. White.

Publicity and Attendance: Cecil Gray, *chairman;* A. F. Forbes, W. A. Murray, and W. F. Nimmo.

Technical Papers: R. C. Bailey, *chairman;* C. P. Knost, A. W. Lee, Jr., and H. E. Wilson.

Student Activities: Claudius Lee, *chairman;* Brinkley Barnett, W. M. Bauer, J. A. Cronvich, H. B. Duling, W. W. Hill, J. S. Jamison, L. M. Keever, W. O. Leffell, N. M. McCorkle, Otto Meier, Jr., J. S. Miller, Jr., W. J. Miller, M. G. Northrop, A. K. Ramsey, S. R. Schealer, E. F. Smith, and F. T. Tingley.

Finance: C. L. Crosby, *chairman.*

NATIONAL ....

Board of Directors Meets

The regular meeting of the board of directors of the American Institute of Electrical Engineers was held at Institute headquarters, New York, N. Y., October 27, 1943.

Upon recommendation of the Standards committee, the board approved for publication Proposed Standard 3, Guiding Principles for the Selection of Reference Values, developed by Standards co-ordinating committee 1, and approved the appointment of D. M. Petty as AIEE representative on sectional committee *C*35 on railway motors, to succeed E. L. Moreland, resigned.

Authorization was given for holding a North Eastern District technical meeting in Boston, during the last two weeks in April 1944, and a Pacific Coast technical meeting in Los Angeles, August 29 to September 1, 1944, inclusive.

Pursuant to actions of the board of directors on October 23, 1942, and June 24, 1943, the following amendments to the bylaws, submitted by the committee on constitution and bylaws, were adopted:

Section 65. "Committee on electronics" added to the list of technical committees.

Section 68. "The Chairman of the Standards committee" added to the list of ex-officio members of the technical program committee.

Section 30. In the list of states forming the various geographical Districts, the States of North Dakota and South Dakota transferred from the listing under District 6 to the listing under District 5.

In accordance with section 22 of the bylaws, five members of the board of directors were selected to serve on the national nominating committee, as follows: M. S. Coover, C. M. Laffoon, T. G. LeClair, E. W. Schilling, W. Ralph Smith. Two alternates were designated, namely, K. B. McEachron and H. S. Osborne.

Upon nomination of the committee on engineering schools of the Engineers Council for Professional Development, the board approved the appointment of V. P. Hessler as an AIEE representative on the delegatory committee for region V, to succeed A. C. Lanier, deceased.

Upon recommendation of a special committee appointed to consider the proposal, the board accepted an invitation for the Institute to become a contributing sponsor of the recently organized Radio Technical Planning Board, and designated George T. Harness as the AIEE representative thereon. The appointment of an alternate was referred to the president.

A progress report was made by the Institute representative, R. G. Warner, on the joint committee, appointed by the joint conference committee of presidents and secretaries of engineering societies, to study the activities of Student Branches under wartime conditions. Decisions have been made to endeavor to enlist the co-operation of the Student Branches of the various societies, through combined meetings, or simultaneous meetings, and to solicit the support of the Army and Navy authorities for Student Branch activities insofar as the Army and Navy trainees are concerned.

Upon recommendation of the chairman of the AIEE committee on Student Branches, the board took action definitely establishing the eligibility for Student enrollment in the Institute of the students in the Army and Navy programs.

An invitation to participate in the commemoration of the 50th anniversary of the American Society of Heating and Ventilating Engineers, in New York, January 31 to February 2, 1944, was accepted, and the president was authorized to act as the Institute's representative on this occasion, or to appoint someone else to attend.

Actions of the executive committee as of September 30, 1943, were reported and confirmed, as follows: 8 applicants transferred and 1 elected to the grade of Fellow; 37 applicants transferred and 23 elected to the grade of Member; 119 applicants elected to the grade of Associate; 421 Students enrolled. Recommendations adopted by the board of examiners at meetings on September 23 and 30 and October 21, 1943, were reported and approved. Upon recommendation of the board of examiners, the following actions were taken: 15 applicants were elected to the grade of Member; 73 applicants were elected to the grade of Associate; 249 Students were enrolled.

The finance committee reported a total income for the year which ended September 30, 1943, of $407,432, and a total expenditure of $312,978. The budget for that year amounted to $352,000. A budget for the appropriation year beginning October 1, 1943, amounting to $370,000, submitted by the finance committee, was adopted.

Present:

President—Nevin E. Funk, Philadelphia, Pa.

Past President—H. S. Osborne, New York, N. Y.

Vice-presidents—A. G. Dewars, Minneapolis, Minn.; J. M. Gaylord, Los Angeles, Calif.; W. J. Gilson, Toronto, Ont.; C. R. Jones, New York, N. Y.; K. B. McEachron, Pittsfield, Mass.; C. W. Ricker, New Orleans, La.; E. W. Schilling, Bozeman, Mont.

Directors—T. F. Barton, New York, N. Y.; M. S. Coover, Ames, Iowa; C. M. Laffoon, East Pittsburgh, Pa.; T. G. LeClair, Chicago, Ill.; C. W. Mier, Dallas, Tex.; S. H. Mortensen, Milwaukee, Wis.; W. B. Morton, Philadelphia, Pa.; W. Ralph Smith, Newark, N. J.; R. G. Warner, New Haven, Conn.

National Treasurer—W. I. Slichter, New York, N. Y.

National Secretary—H. H. Henline, New York, N. Y.

Minutes of the meeting of the board of directors held August 4, 1943, were approved.

Other matters were discussed, reference to which may be found in this or future issues of *Electrical Engineering*.

Winter Technical Meeting to Feature War and Postwar Topics

Arrangements are in progress for the winter technical meeting, which will be held in New York, N. Y., January 24–28, 1944, with headquarters in the Engineering Societies Building. The Edison Medal will be presented during this meeting.

A number of technical sessions and technical conferences will be held to present subjects which are of aid to the war effort and which also will be of value in the postwar period. Continuing the co-operative work of co-ordinating meeting schedules the Institute of Radio Engineers' winter technical meeting will be held concurrently on January 28 and 29. The programs are being arranged so as to afford three days of continuous interest for members concerned with radio and communication work.

Thursday evening, January 27, a joint AIEE–IRE session is planned with Major General Roger B. Colton, chief, engineering and technical service, Signal Corps, United States Army, the principal speaker. Major General Colton will discuss enemy communication equipment.

SMOKER

In order that those attending the winter technical meeting may have an opportunity to get together with their friends under conditions where all may relax and pass a

Future AIEE Meetings

Winter Technical Meeting
New York, N. Y., January 24–28, 1944

North Eastern District Meeting
Boston, Mass., April 1944

Summer Technical Meeting
St. Louis, Mo., June 26–30, 1944

thoroughly enjoyable evening it has been decided this year to have a smoker, Tuesday evening, January 25, at the Hotel Commodore. Insofar as possible, all conditions surrounding the affairs of previous years will be provided. Tickets, including dinner and show, will be five dollars per person. Tables for ten can be reserved by advance payment. Checks should be made out to "Special Account, National Secretary, AIEE."

THEATER TICKETS

Because of the difficulty in obtaining theater tickets under present conditions, only a limited number of such tickets will be made available for Thursday evening, January 27. In selling these tickets preference will be given to out-of-town members. Members desiring tickets for specific performances may write to Institute headquarters, enclosing checks made out to "Special Account, National Secretary, AIEE," and every effort will be made to secure seats. If alternative shows or dates are acceptable, these should also be given.

COMMITTEE

The personnel of the 1944 winter technical meeting committee, which is making the arrangements, is as follows:

J. F. Fairman, *chairman;* W. J. Barrett, F. A. Cowan, J. L. Callahan, M. D. Hooven, C. R. Jones, R. A. Jones, F. V. Magalhaes, C. S. Purnell.

Contents of December 1943 Supplement Announced

Technical papers to supplement those published in the monthly Transactions sections of *Electrical Engineering* for July through December will appear in the December 1943 "Supplement to Electrical Engineering—Transactions Section." This supplement will contain 12 papers, their discussions, and the discussions of technical papers published in the July–December monthly sections. Publication of this supplement completes the publication of papers and discussions presented at the 1943 AIEE North Eastern District meeting in Pittsfield, Mass., the 1943 AIEE summer technical meeting in Cleveland, Ohio, and the 1943 AIEE Pacific Coast meeting in Salt Lake City, Utah.

Copies of the supplement will be available within a few weeks and will be mailed to those who entered advance orders. Others may obtain copies at 50 cents each from the AIEE order department, 33 West 39th Street, New York 18, N. Y.

Papers appearing in the December 1943 Supplement, abstracts of which have been published in *Electrical Engineering* in advance of the meetings, are:

43-66—The Frequencies of Natural Power Oscillations in Interconnected Generating and Distributing Systems; *Reinhold Rüdenberg (M '38).* Abstracted in April 1943 issue, page 172.

43-67—The Sorocabana Railway Electrification; *Durval Muylaert.* Abstracted in April 1943 issue, page 172.

43-101—Interim Report on Characteristics and Performances of Conductors for Supervisory Control and Telemetering; *AIEE subcommittee on characteristics and performance of conductors for supervisory control and telemetering.* Abstracted in June 1943 issue, page 266.

43-76—Pole-Face Loss in Solid-Rotor Turbine Generators; *W. W. Kuyper (A '34).* Abstracted in June 1943 issue, page 268.

43-77—The Effect of Corona on Coupling Factors Between Ground Wires and Phase Conductors; *G. D. McCann (A '38).* Abstracted in June 1943 issue, page 269.

43-85—Safe Ratings for Overhead Line Conductors; *L. M. Olmstead (M '39).* Abstracted in the June 1943 issue, page 269.

43-80—Application of Carrier to Power Lines; *F. M. Rives (M '41).* Abstracted in the June 1943 issue, page 269.

43-90—Theory of Rectifier—D-C Motor Drive; *E. H. Vedder (M '35), K. P. Puchlowski (A '43).* Abstracted in the June 1943 issue, page 269.

43-137—Turn Ratio of the Capacitor Motor; *T. C. MacFarland (M '32).* Abstracted in the August 1943 issue, page 375.

43-140—Radio-Noise Elimination in All-Metal Aircraft; *Fred Foulon (M '42).* Abstracted in the August 1943 issue, page 374.

43-144—Rectifier Drive for D-C Motors; *K. P. Puchlowski (A '43).* Abstracted in the August 1943 issue, page 375.

43-141—Applications of 720-Cycle Carrier to Power Distribution Circuit; *J. L. Woodworth (A '43).* Abstracted in August 1943 issue, page 275.

Formation of Aircraft Groups Promoted by Sections Committee

The AIEE Sections committee, George W. Bower (M '40) chairman, is promoting the formation of aeronautical technical discussion groups in localities where there is evidence such groups are needed. In addition, the Sections committee, jointly with the membership and technical program committees, is encouraging meetings on this subject in other Sections where the organization of a separate group is not possible because of the lack of available members. Two aircraft discussion groups have already been formed and are functioning, one in the Los Angeles Section, and one in the Philadelphia Section.

Lieutenant Colonel T. B. Holliday (A '41) United States Army Air Corps, Wright Field, Dayton, Ohio, chairman of the air transportation committee, commenting on the drive for aircraft discussion groups, said:

"This activity is worth while and in all probability will be rewarded with a very active interest on the part of Institute members. Most members realize the magnitude of the Air Forces' task in the current war effort and appreciate the success their operations are meeting. A natural result of this is a very intense curiosity regarding the equipment which makes these operations possible.

"This equipment represents some departures from conventional practice insofar as electrical design is concerned, and it is expected to function under conditions which no other type of electrical equipment has ever been expected to meet. Since an airplane can travel from one point to any other point of the world within 60 hours, equipment cannot be designed for local operating conditions, but must be satisfactory in the Arctic cold and in desert heat, must withstand salt-water corrosion and exposure to sand and dust, and must function correctly at 65 degrees below zero and 135 degrees above, at sea level and at altitudes as high as 40,000 feet. At the same time, weight and space must approach a minimum, and adequate reliability must be provided.

"The electrical engineers of the industry have accomplished many miracles in design in meeting these new and difficult requirements. The progress which has been made in this fashion must be of considerable benefit to commercial applications which will follow the war. Therefore, all engineers in the industry will follow the activities of the aviation group with a great deal of interest."

The interest of aircraft electrical engineers in AIEE may be judged from the fact that approximately 150 new members have joined the Los Angeles Section since the formation of the aircraft division. This division offers the aircraft electrical engineer a medium for exchange of ideas and for the discussion of developments and their associated problems. The aircraft division of the Los Angeles Section came into existence March 9, 1943. The first meeting was primarily an organizational affair, held for the purpose of determining the form and frequency of meetings and the most desirable topics for discussion. It was decided to have speakers at future meetings cover a specific subject in a fixed time and then to open the meeting for general discussion. Scheduled meeting dates were not to be rigid, but rather were to be made to suit the occasion. Responsibility for the assembly and presentation of program material were to be delegated to a program committee. It was agreed that the meetings would be dinner meetings whenever possible.

At the first technical meeting, May 26, 1943, Mr. Gagnier of North American Aviation, Inc., discussed the subject, "Aircraft Network Systems and Circuit Protection." The second technical session, July 21, 1943, featured talks on "Protection Devices" by Mr. Stevens of Consolidated-Vultee Aircraft Corporation, and Julian Rogoff of the Burndy Engineering Company. This topic dealt with circuit breakers, fuses, and replaceable links for circuit protection. Many of the problems discussed at this meeting were very similar to those encountered in power-company work. The last meeting of the aircraft division, July 21, 1943, covered the subject, "Electrical Control Equipment for Aircraft." Speakers for the evening were Mr.

Crago and Mr. Bradish of the General Electric Company, and Mr. Lear, Chief Engineer of Lear-Avia, Inc. Demonstrations by both General Electric and Lear-Avia companies of many applications of control systems were featured at the meeting. Attendance at these aircraft meetings has ranged between 200 and 300.

Tentative plans have been made for meetings of the Los Angeles aircraft division to be held January 26, February 23, and March 22, 1944. The January 26 meeting will deal with the subject of "Aircraft Instrumentation" which includes design of installation, installation problems, and consideration of the instruments themselves. G. F. Burnett of the Lockheed Aircraft Corporation will be in charge of this session. February 23 the topic will be "Open-Wiring Design Consideration in Military Aircraft." Fred Foulon (A '30) of the Douglas Aircraft Company, el Segundo Division, will be responsible for this program. The subject for the March 22 meeting will be "Electrical Power Drives for Aircraft Functions." Ernest Seifkin of the Vega Aircraft Company will handle the program for that evening.

The Philadelphia Section has sponsored discussion group meetings in the interest of the aviation industry for the past two years. Through the phenomenal growth of the aviation industry in its area, the Philadelphia Section saw the need for a discussion of aviation subjects and formed the aircraft electrical engineering discussion group. This group has held two meetings to date and has planned three meetings for the 1943–44 season at which technical developments in the aircraft industry were and will be discussed. To obtain greatest benefit for the members, some of the meetings are held jointly with the Franklin Institute. The subject of aircraft has attracted such widespread interest that the board of governors devoted the main meeting, October 11, 1943, to an aircraft program. Lieutenant Colonel T. B. Holliday, chief, electrical branch, equipment laboratory, engineering division of the United States Army Air Corps spoke on the subject, "Development and Trends of Electrical Systems on Military Aircraft." This talk was considered one of the most outstanding talks ever given before the Section.

These aviation activities bring to the Philadelphia and Los Angeles Sections groups extremely enthusiastic on the subject of aviation. Through the enthusiasm shown by the members of these Sections, the tie between AIEE and the aviation industry becomes increasingly stronger.

ABSTRACTS ...

TECHNICAL PAPERS previewed in this section will be presented at the AIEE winter technical meeting, New York, N. Y., January 24–28, 1944, and will be distributed in advance pamphlet form as soon as they become available. Copies may be obtained by mail from the AIEE order department, 33 West 39th Street, New York 18, N. Y., at prices indicated with the abstract; or at five cents less per copy if purchased at AIEE headquarters or at the meeting registration desk.

Mail orders will be filled
AS PAMPHLETS BECOME AVAILABLE

Air Transportation

44-5—Aircraft Signal Systems; *R. A. Rugge (M '36). 15 cents.* Because the selection of aircraft warning devices has not, in most signal installations, provided the pilot with dependable warnings of dangerous conditions, this paper proposes that aircraft signal devices should be designated and installed in compliance with the pilot's requirements as expressed in three design principles. The paper describes the weaknesses of current signal devices; it recommends signal-light indicators that are bright enough to be seen under direct sunlight; and it illustrates and describes an automatic dimming control that operates as a function of natural light, independent of the pilot's actions.

44-7—Electric Circuits and the Magnetic Compass; *R. C. Burt (F '43), H. R. Beck (Nonmember). 15 cents.* In the airplane large deviations, that is, errors of the magnetic compass may be caused by current-carrying conductors. Such deviations are becoming more important with global flights and the ever-increasing use of steel and electricity. Though space limitations prevent desirable separation of magnetic compass and electric wires, the wiring can be arranged so that deviation is minimized by use of two-wire circuits, divided-conductor single-wire systems, self-compensating placement, and by the use of series-compensating coils. Equations useful in wiring layout are easily developed from the well-known laws of electromagnetism, and such calculations develop limiting tolerable distances for uncompensated wires. This treatment, plus thorough demagnetizing process control, will improve the compass installation greatly, be it direct-reading or remote-indicating, with consequent improvement in navigation and safety.

Education

44-3—Cultural Training of the Engineer; *A. Boyajian (F '26). 15 cents.* Parents, society, and industry, even the armed services, expect engineers to receive a certain amount

Members of the Akron Section are pictured on the steps of the Goodyear Research Laboratory which they visited recently as part of the program of their first fall meeting. After dinner and a motion picture on steam turbines, the meeting concluded with a round-table discussion. The meeting program was arranged by R. F. Snyder (A '40) electrical engineer for the Goodyear Tire and Rubber Company

of cultural training, and for many years the engineering colleges have provided such courses. In this paper the opinion is expressed that the cultural courses have not been so well planned as the technical. The basic cultural needs of the student are: an efficient technique for study, skill in dealing with himself and others, and ability in self-expression, written and oral. These must be not only understood but also converted into habit patterns; and, therefore, their acquisition may not safely be delayed beyond the freshman year. For this objective, the foundation course ought to be practical psychology. The topics to be emphasized are: habit, with appropriate exercises; human motivation; principles of thinking; psychology of attention; and psychology of public speaking and good writing.

Electrical Machinery

44-1—Design of Starting Windings for Split-Phase Motors; *T. C. Lloyd* (*A '31*), *J. H. Karr* (*A '32*). *15 cents.* In the design of split-phase induction motors, the main winding usually is fixed by considerations of the running performance. The starting winding must be so chosen that it results in acceptable values of starting torque and current. Indirect trial-and-error methods which leave the designer in doubt as to whether a readjustment of the winding parameters would not have produced a more effective winding, frequently are used to determine the starting winding. A more direct method, whereby starting winding constants are obtained as functions of main-winding terms, results in simplified calculations yielding the desired performance characteristics, with a quick check on winding effectiveness provided by reference to curve sheets.

44-2—Some Aspects of Electric-Motor Design—Polyphase-Induction-Motor Design to Meet Fixed Specifications; *T. C. Lloyd* (*A '31*). *30 cents.* Many calculation processes whereby the electrical designer can predict the performance of his machine while it is still in the design stage are available. Few data are available for determining the initial dimensions of the proposed design aside from the use of the familiar D^2L formulas for rotating machines. A modern approach to design problems can be defined as one in which the electric and magnetic circuits are solved directly in terms of the performance to be met. A critical examination of the D^2L formula for polyphase induction motors reveals its shortcomings in favor of a process based on design in magnetic circuits of fixed contour which determines at once the windings to meet performance specifications. The point of view, illustrated by polyphase-induction-motor design, is applicable to a wider range of design processes.

44-6—Asymmetrical Loading of Three-Phase Three-Winding Transformer Banks; *P. K. Denissov* (*Nonmember*). *15 cents.* It is accepted Soviet practice to carry three-phase industrial load and single-phase traction load on a single three-phase three-winding transformer. For a given combination of loads it is necessary to determine the proper capacity relationships of the three windings. This paper presents an analysis of the problem by the methods of symmetrical components. The data obtained from this analysis were used in setting up the standards for selection of transformer size for combined three-phase industrial and single-phase traction loading.

44-10—Transformer Magnetizing Inrush Currents and Influence on System Operation; *L. F. Blume* (*F '39*), *G. Camilli* (*F '43*), *S. B. Farnham* (*M '42*), *H. A. Peterson* (*M '41*). *30 cents.* This paper summarizes the results of investigations evaluating the practical significance of power-transformer magnetizing inrush currents. This phenomenon, long recognized, provides a basis for renewed emphasis on questions regarding the effect of inrush currents on system voltages, fuses, and so forth, particularly in view of the present trend toward the use of high-silicon strip steel in the design of power transformers. Miniature system tests, tests on power transformers, and calculations are used as a basis for arriving at definite conclusions. Several corrective means, possibly of value in extreme cases, are discussed with quantitative data included.

Electronics

44-8—Electronically Controlled Dry-Disk Rectifier; *Allen Rosenstein* (*A '41*), *H. N. Barnett* (*Nonmember*). *15 cents.* Selenium-oxide rectifiers are assuming an increasingly important position in the low-voltage d-c power field. The outstanding characteristics of these rectifiers are ruggedness, high efficiency, and maintenance-free operation. The disadvantages are lack of fine voltage variation and only fair voltage regulation under load. This paper describes an electronic control which eliminates these handicaps and makes possible dry-disk rectifiers which possess smoothly variable output voltage that is maintained practically constant under load for any setting. To achieve the regulation, the rectifier voltage is compared against a constant comparison voltage, amplified, and used to shift the phase of the grid voltage of reactor-saturating thyratrons. The saturable reactors, in turn, control the rectifier voltage and compensate properly for voltage changes caused by variations in either load or primary line voltage. Circuit diagrams are shown and performance curves of existing installations given.

44-11—Analysis of Rectifier Circuits; *E. F. Christensen* (*A '39*), *C. H. Willis* (*F '42*), *C. C. Herskind* (*M '40*). *30 cents.* This paper presents a standard procedure for the analysis of rectifier circuits. While the general principles underlying the analysis of rectifier circuits have been covered quite fully in the literature, there is not available a complete analysis giving the essential formulas in a systematic and unified form readily usable by engineers. It is the object of this paper to present such a treatment. The paper describes the methods used in determining the wave forms throughout the rectifier circuit and in deriving the mathematical formulas which apply. While the analysis is made for the widely used delta double-wye circuit, the method is generalized so that it may be applied to all types of rectifier circuits. Rectifier-circuit calculations and presentation of calculated data are facilitated by expressing the various characteristics in terms of the reactance factor. A comprehensive set of characteristic curves expressing the various quantities in terms of reactance factor is included. These curves should prove useful to design, application, and operating engineers.

Industrial Power Applications

44-18—Inverter Action on Reversing of Thyraton Motor Control; *H. L. Palmer* (*A '42*), *H. H. Leigh* (*Nonmember*). *25 cents.* The thyratron rectifier now being applied to the control of d-c motors offers a constant-current decelerating characteristic, similar to and combined with its accelerating characteristic, which can be utilized in reversing and regenerative braking of the motor. By retarding the firing of the thyratrons and reversing the generated voltage of the motor with respect to the tube circuit, the thyratrons can be made to invert the d-c power generated in the motor by the rotational energy of the motor and its connected load, into the a-c system at a constant d-c current. During inversion the d-c current can be controlled by the same means with which it is controlled during rectification. After the motor is decelerated, the circuit can be effectively opened, or it can be shifted to rectifier operation by phase control of the thyratrons to accelerate the motor in the reverse direction.

44-19—A New Electronic Hoist Drive for Cranes; *M. A. Whiting* (*A '07*). *20 cents.* It is generally recognized that for cranes and some other classes of hoists an electric drive is desirable having the inherent characteristic that a heavy load is lowered at a low speed but a light load or the empty hook is lowered at a much higher speed. A system having these characteristics has been developed and successfully applied. This paper explains the theory and design of the system and presents results of tests.

Instruments and Measurements

44-4—An Instrument for the Measurement of Large Alternating Currents; *Walther Richter* (*F '42*). *15 cents.* An arrangement for the measurement and oscillographic recording of large alternating currents is described. The device consists essentially of a flexible air-core transformer, small enough to be easily linked with the low-voltage high-current circuits usually met with in practice, and furnishing

a voltage proportional to the line integral of the magnetic-field intensity around the current to be measured. The theory of the air-core transformer and the fundamental difference between it and the regular current transformer are discussed. It is shown that for currents of high harmonic content the voltage obtained from any air-core transformer is not a direct measure of the current and cannot be used for oscillographic recording. However, by applying the voltage induced in the air-core transformer to an integrating circuit, the resulting voltage can be made very nearly a replica of the original current wave; the auxiliary circuit and amplifier needed to accomplish this are described.

44-9—Polarized Light Servosystem; *T. M. Berry (A '43).* *15 cents.* A new servosystem has been developed in which the connecting link between the primary and secondary shafts is polarized light whose plane of polarization rotates with the primary shaft. This makes possible the accurate following of the rotation of the primary shaft without any load being imposed on it. This servosystem has been applied successfully in several electro-mechanical calculating machines.

44-16—A New Differential Analyzer; *H. P. Kuehni (M '43), H. A. Peterson (M '41).* *25 cents.* A new 14-integrator differential analyzer recently has been put into operation by the General Electric Company at Schenectady, N. Y. While this analyzer retains the mechanical interconnection system consistent with established differential-analyzer design practice, significant changes have been made in the integrators themselves. While mechanical torque amplifiers are used in existing analyzers, this analyzer uses a novel Polaroid-light-beam photoelectric follow-up system which has characteristics highly desirable for this purpose. The use of this system has permitted important changes in integrator design.

Power Generation

44-17—Lightning Protection of Rotating Machines; *G. D. McCann (A '38), E. Beck (M '35), L. A. Finzi (A '40).* *30 cents.* A detailed study of methods for protecting rotating machines connected to exposed transmission lines has been made. This has been based upon the present knowledge of lightning discharge currents. With equivalent circuits used to represent sections of overhead lines and cables, measurements were made of the machine terminal voltages permitted by various circuit conditions when machines are connected directly and through cables or transformers to overhead lines. A study also was made of the surge voltages which can be transferred through cables for various conditions of sheath grounding. Measurements were made of the surge-voltage conditions which can be set up within machine windings under various conditions of machine grounding. This was for the purpose of determining the best method of protection for machines not grounded through sufficiently low impedance to be protected adequately by methods applicable to solidly grounded machines. Specific recommendations are given for improved methods of machine protection under various circuit conditions.

Power Transmission and Distribution

44-12—Steady-State Stability of Synchronous Machines as Affected by Voltage-Regulator Characteristics; *C. Concordia (M '37).* *25 cents.* This paper presents a method for calculating directly the gain in steady-state stability with a given voltage regulator and excitation system, and also for predetermining the optimum regulator and excitation-system characteristics to obtain maximum gain in stability for a given synchronous machine or motor-generator set. Results of calculations by the methods developed are given for some typical systems. An important application of the analysis is the case of electric ship propulsion, where the size and weight of the generator and motor are determined in part by steady-state stability requirements.

44-13—Transmission and Relaying Problems on the Fort Peck Project; *Erik Floor (Nonmember), H. N. Muller (M '43), S. L. Goldsborough (M '43).* *20 cents.* This paper discusses application problems and their solution in connection with power transmission and relaying for the Fort Peck project. Four variations of systems were studied with respect to steady-state and transient stability and the reasons for selecting the particular system used are discussed. An interesting application of a water rheostat as a substitute for a water tower is described. The relaying problems arising from the extreme length of the line are discussed and a modified impedance-relay system is described. Proper ground-relay protection involved the use of three-phase grounding switches at both ends of the line. While the present relaying system does not involve carrier current, a variation of the present system suitable for carrier-current operation is described. Also, other variations in relay characteristics suitable for long lines in general are discussed.

44-15—Regulation of A-C Generators with Suddenly Applied Loads; *E. L. Harder (M '41), R. C. Cheek (A '42).* *30 cents.* Many small fixed and mobile power plants have been built recently for wartime applications. In such applications the major portion of the load frequently consists of one or more induction motors of ratings amounting to an appreciable fraction of the rating of the generator supplying them. The low-power-factor starting currents of these motors may amount to several times rated generator current, producing severe terminal voltage drops during starting periods. Such voltage drops, which may result in the dropping out of motor starters or the pulling out of motors already running because of low terminal voltage, can be reduced considerably by the use of quick-acting regulating and exciting systems. It is essential in the application of such systems to be able to predict maximum generator terminal voltage drops when various amounts of load are applied. In this paper curves are presented that give directly the maximum voltage drops to be expected at the terminals of a-c generators for a wide range of generator, exciter, and load characteristics. Selection of the combinations of characteristics for which curves are presented is based on a study of the effect of each individual factor having a bearing on maximum voltage drops. The voltage-drop curves are based on equations that yield directly the minimum voltage of the generator voltage–time curve following sudden application of load. The derivation of these equations is given in the paper. Test results are presented to support the derivation.

44-14—A Short-Cut Method of Estimating TIF of Power Systems with Rectifier Load; *C. W. Frick (A '19).* *20 cents.* A short-cut method is described for estimating the telephone influence factor of power systems with rectifier load, and working curves are given for 60-cycle and 25-cycle systems. The commonly used phase arrangements are covered including 6 and 12 phases as used for the smaller rectifier loads and 24 or more phases as used for the largest rectifier installations. Illustrative examples and comparisons with test results are included. Data pertaining to the effect of a proposed revision of the *TIF* weighting curve on these calculations are given in the appendix.

PERSONAL • • • •

J. F. O'Connor (M '22) formerly vice-president in charge of the electric operating department, Toledo (Ohio) Edison Company, has been appointed vice-president and assistant general manager of the company. A graduate of the University of Colorado, Mr. O'Connor became associated with the company in 1919 when the Acme Power Company of which he was vice-president and general manager was absorbed by the Toledo Edison Company. In 1922 he was given charge of the general engineering department, and in 1923 made manager of the electric department. From 1929 to 1935 he served as general superintendent and since 1936 he has been vice-president in charge of operations. **H. H. Kerr** (A '17, M '26) formerly superintendent of the electrical department, has been named general superintendent of engineering operation, maintenance, and construction. Mr. Kerr entered the employ of the Edison company in 1929 after 14 years with the Public Service Company of Colorado, Denver. **P. R. Knapp** (A '18, F '38) formerly superintendent of electric distribution and transmission, has been made assistant general superintendent.

Mr. Knapp, a graduate of Worcester Polytechnic Institute, joined the Edison company in 1919. **A. F. Maxwell** (A '31) formerly superintendent of electric distribution, Fremont, Ohio, succeeds Mr. Knapp as superintendent of electric distribution and transmission.

J. T. Frankenberg (A '17, M '13) depreciation studies engineer, New England Telephone Company, Boston, Mass., has retired. Mr. Frankenberg received his mechanical engineering degree from Ohio State University in 1899. During 1899 and 1900 he installed instruments and switchboards throughout the state of Ohio for the Central Union Telephone Company. From 1900 to 1903 he held the positions of wire chief and inspector in the Columbus, Ohio, office of the Central Union company. He was employed as telephone engineer by the Central District and Printing Telegraph Company, Pittsburgh, Pa., from 1903 to 1905, and by the American Telephone and Telegraph Company, Boston, Mass., during 1905 and 1906. In 1906 he became chief engineer of the Providence (R. I.) Telephone Company. From 1916 to 1922 he was superintendent of plant for that company, and was named division superintendent of plant for the New England Telephone and Telegraph Company. Since 1924 he has been a staff engineer of that company.

M. M. Rockwell (A '28, M '35) product research engineer, Lockheed Aircraft Corporation, Burbank, Calif., has been awarded the $100 industrial prize of the American Welding Society as co-author of the paper, "The Effect of Welding Spacing on the Strength of Spot-Welded Joints," published in the October 1942 *Welding Journal*. Born July 25, 1902, in Philadelphia, Pa., Mrs. Rockwell received the degree of bachelor of science from the Massachusetts Institute of Technology in 1925 and the degree of electrical engineer from Stanford University in 1926. In 1926 she joined the Southern California Edison Company Ltd., Los Angeles, and worked as testman, laboratory technician, and technical assistant until 1931. In 1931 she was made assistant electrical engineer for the Metropolitan Water District of Southern California. She entered the employ of the Lockheed Corporation in 1938 as plant electrical engineer. In 1929 she was awarded the AIEE initial paper prize for the Pacific Coast District.

H. W. Tenney (M '36, F '43) assistant director, research laboratories, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., has been made assistant to the vice-president in charge of the company's Pittsburgh divisions. Mr. Tenney has been associated with the Westinghouse company since 1920 when he was employed as a draftsman in the switchboard department. Transferred to the materials and process engineering department in 1922, he held the positions of laboratory foreman, section engineer, and division engineer in charge of the high-voltage, engineering, high-power, chemical and physical, and central engineering laboratories. In 1936 he became manager of the central engineering laboratory department, and manager of the new-products division when it was organized in 1937. He was made manager of the engineering laboratories and standards department in 1938 and assistant director of research laboratories in 1941.

W. L. Cisler (M '35) formerly assistant chief engineer, Public Service Gas and Electric Company, Newark, N. J., has resigned to join the Detroit (Mich.) Edison Company. Graduating from Cornell University in 1922, Mr. Cisler entered the employ of the Public Service Gas and Electric Company as a cadet engineer later becoming test engineer at the company's Marion and Kearny, N. J., generating stations; assistant chief engineer at its Paterson, N. J., station; and chief engineer at the Marion station. In 1931 he was made planning and installation engineer; in 1935 general superintendent; in 1936 assistant general manager; and in 1938 assistant chief engineer. In 1941 he was appointed to the staff of the power coordinator of the Office of Production Management, Washington, D. C., and later served as chief of equipment production in the power section of the Office of War Utilities.

K. W. Jarvis (A '25, M '34) formerly consulting engineer, Winnetka, Ill., is now vice-president of the Sheridan Electro Corporation, Chicago, Ill. Mr. Jarvis commenced his career in the radio field in the radio laboratory of the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., in 1923. During subsequent employment with several radio companies he was active in the evolution of radio design. He has held the positions of assistant chief engineer and engineering director for the Zenith Corporation, Chicago, Ill., from 1932 to 1935; vice-president of the Norwalk Engineering Corporation, South Norwalk, Conn., during 1936 and 1937; and chief engineer of the Seeburg Radio Corporation, Chicago, Ill., in 1938. Since 1939 he has practiced as consulting engineer. He is a member of the Institute of Radio Engineers and has published many technical articles.

W. F. Hess (A '32, M '41) assistant professor of metallurgical engineering, and head of the welding laboratory, Rensselaer Polytechnic Institute, Troy, N. Y., has been awarded the $100 university prize of the American Welding Society as co-author of the paper "The Spot Welding of 0.040 In. SAE X-4130 Steel," published in the October 1942 *Welding Journal*. Doctor Hess was graduated from Rensselaer Polytechnic Institute in 1925 with the degree of electrical engineer and in 1928 received the degree of doctor of engineering from that institution. He served as instructor in electrical engineering and physics at Rensselaer Polytechnic Institute from 1928 to 1930. In 1930 he was made assistant professor in those subjects and in 1938 became assistant professor in metallurgical engineering and head of the welding laboratory. He is a member of the Society for the Promotion of Engineering Education.

F. M. Farmer (A '02, F '13) vice-president and chief engineer, Electrical Testing Laboratories, New York, N. Y., has been elected president of the United Engineering Trustees, Inc. He has represented AIEE on the board of the UET since 1937. Mr. Farmer has been connected with the Electrical Testing Laboratories since 1903 as technical assistant, engineer, chief engineer, and vice-president. A past president of AIEE, the American Society for Testing Materials, and the American Welding Society, he is also a past chairman of the Engineering Foundation and the standards council of the American Standards Association. In addition he is a fellow of the American Association for the Advancement of Science and a member of the American Society of Mechanical Engineers, and the Illuminating Engineering Society.

F. J. Vogel (A '21, M '41) formerly consulting engineer, Westinghouse Electric and Manufacturing Company, Sharon, Pa., has been appointed professor of electrical engineering at Illinois Institute of Technology, Chicago. Professor Vogel received the degree of bachelor of science in electrical engineering from Massachusetts Institute of Technology in 1915 and, after serving in the United States Navy, entered the employ of the Westinghouse company in 1919 as transformer design engineer. Later he was made division engineer in charge of large power transformer design and in 1940 consulting engineer for the transformer division. Author of many AIEE papers and other technical articles, he has served on the electrical machinery committee, and the electrometallurgy and electrochemistry committee, as well as various subcommittees.

E. A. Johnson (A '35) plant extension engineer, Southern New England Telephone Company, New Haven, Conn., has been appointed chief engineer. Graduating from Pratt Institute in 1919, Mr. Johnson worked for a short time for the Toledo (Ohio) Railways and Light Company and for the Winchester Repeating Arms Company, New Haven. In 1920 he entered the employ of the telephone company as an engineering assistant in the office of the traffic engineer. In 1930 he became traffic engineer. He was assigned to the chief engineer's office as plant extension engineer in 1934. **R. W. Purssell** (A '30) engineer of construction program, succeeds Mr. Johnson as plant extension engineer. He has been employed by the telephone company since 1929.

W. W. Ege (A '23, M '32) formerly district manager, Copperweld Steel Company, Chicago, Ill., has been made general manager of sales for that company at Glassport, Pa. Mr. Ege, who is a graduate of Lewis Institute, has been employed by the Copperweld company since 1925, first in the engineering department, and since 1930 as manager of the Chicago office. **Henry Oberle** (A '23, M '36) formerly superintendent of electric distribution and transmission, Queens Borough Gas and Electric Company, New York, N. Y., has been appointed eastern sales manager for the Copperweld company. A graduate of the University of Montana, he joined the Queens Borough company in 1925 as assistant superintendent of distribution and in 1926 was made superintendent.

L. F. Fuller (A '12, F '23) formerly head of the electrical engineering department, University of California, Berkeley, has joined the engineering department of the Joshua Hendy Manufacturing Company, Sunnyvale, Calif. Professor Fuller had been on the staff of the University of California since 1930. As chief electrical engineer for the Federal Telephone Company, New York, N. Y., during World War I, he designed and supervised manufacture and installation of transoceanic apparatus for the United States Government in the Philippine and Hawaiian Islands, Alaska, Guam, the Canal Zone, Porto Rico, and France. From 1917 to 1919 he was a member of the antisubmarine group of the National Research Council.

E. S. Lee (A '20, F '30) engineer, general engineering laboratory, General Electric Laboratory, Schenectady, N. Y., has been elected chairman of the Engineers Council for Professional Development. Mr. Lee holds the degrees of electrical engineer (1913) from the University of Illinois, and bachelor and master of science from Union College (1915). He entered the general engineering laboratory of the General Electric Company in 1919, becoming assistant engineer in 1928 and engineer in charge in 1931. A former vice-president (1940–42) of AIEE, he is also a member of the American Society of Mechanical Engineers, the Institute of Radio Engineers, the Newcomen Society, Etta Kappa Nu, Tau Beta Pi, and Sigma Xi.

Arthur Slepian (A '38) formerly general manager, Wheeler Insulated Wire Company, Bridgeport, Conn., has been appointed vice-president of the company. Mr. Slepian received the degree of bachelor of science from Massachusetts Institute of Technology in 1922 and the degree of master of science from Harvard University in 1923. From 1923 to 1925 he was entered in the student course of the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa. In 1925 he joined the Wheeler company to work on the development of new types of wire and in 1926 became assistant general manager. He is a member of the American Institute of Mining and Metallurgical Engineers.

S. M. Dean (A '25, M '41) formerly chief assistant superintendent of electrical system, Detroit (Mich.) Edison Company, has been appointed chief engineer of the system. Mr. Dean was graduated from Michigan State College in 1914 and entered the test course of the General Electric Company, Schenectady, N. Y., in 1916. He was transferred to the general office of the commercial department in 1918 and became sales engineer in 1919. In 1924 he was made assistant manager of the company's Detroit office. Joining the Detroit Edison company in 1925, he became senior electrical engineer in 1927. In 1930 he was made chief assistant superintendent.

Vannevar Bush (A '15, F '24) president, Carnegie Institution of Washington (D. C.), recently was awarded the Holley Medal of the American Society of Mechanical Engineers for "leadership and inventions in easing applied mathematics from computational barriers; and particularly for the differential analyzer that has enlarged differential equation analysis, thereby providing improved analyses in such fields as atomic physics, cosmic rays, ballistics, and automatic control and improved opportunities for understanding various electrical, mechanical, and other physical systems associated with engineering."

F. S. Wilhoit (A '03) purchasing agent, Cutler-Hammer, Inc., Milwaukee, Wis., has retired. Mr. Wilhoit commenced his career as construction engineer for the Sprague Electric Company, Chicago, Ill., in 1898. In 1902 he entered the employ of the Cutler-Hammer as sales engineer and later that year was made assistant superintendent of the company, the position he held until 1909. From 1909 to 1924 he was manager of the printing equipment department. After a period as sales engineer from 1925 to 1932, he became purchasing agent of the company in 1933.

G. W. Spaulding (A '24, M '32) formerly assistant chief engineer, Pennsylvania Water and Power Company, Baltimore, Md., has been appointed vice-president of the company. Mr. Spaulding entered the employ of the Pennsylvania Water and Power Company in 1924 as test engineer. In 1925 he was made assistant chief of tests in the company's hydroelectric and steam plants at Holtwood, Pa. From 1928 to 1938 he held the position of assistant to the general superintendent of the company. In 1939 he became assistant chief engineer.

W. J. Piper (M '39) engineer, meter department, Detroit (Mich.) Edison Company, has been appointed assistant superintendent of meters for that company. A graduate of the University of Michigan, Mr. Piper was a technical assistant in the meter department from 1923 to 1929. Since 1929 he has worked as an engineer in the department. **O. E. Hauser** (A '20, M '32) has been appointed senior engineer in the meter department. Mr. Hauser also has been an engineer in the meter department since 1929.

E. H. Snyder (A '27, M '33) formerly assistant to the chief engineer, Public Service Electric and Gas Company, Newark, N. J., has been appointed assistant chief engineer of the electrical-engineering department. A graduate of Lehigh University, Mr. Snyder entered the employ of the Public Service company as a cadet engineer in 1923. Assigned to the distribution engineer's office in 1926, he took charge of a planning group in that office in 1930. In 1937 he was appointed assistant to the chief engineer.

H. R. Winters (A '37) formerly assistant sales manager of the central division, Oklahoma Gas and Electric Company, Sapulpa, has been placed in charge of sales for that division. Mr. Winters has been employed by the company since 1928 and has been attached to the commercial office of the central division since 1937. **R. L. McIntire** (A '34) formerly chief electrician at Sapulpa, has been appointed assistant superintendent of the company's Shawnee district.

A. S. Walker (A '27) formerly operating engineer, New England Power Service Company, Boston, Mass., has been appointed assistant vice-president. He was engaged first in maintenance work for the company during 1913. In 1914 he became station operator and in 1916 load dispatcher. He was made chief load dispatcher in 1919 and superintendent of distribution in 1921. From 1930 to 1941 he was assistant to the general manager. In 1942 he was made operating engineer.

E. K. Goss (A '33) formerly general plant supervisor, Indiana Bell Telephone Company, Indianapolis, has been appointed acting general plant manager. Mr. Goss has been employed by the Indiana Telephone company since 1920 as telephone facilities engineer, and plant engineer and supervisor. **J. W. Quinlan** (M '35) formerly general plant employment supervisor, has been appointed division plant superintendent.

L. R. Mapes (M '29, F '37) formerly general manager, state area, Illinois Bell Telephone Company, Chicago, has been appointed assistant vice-president. Mr. Mapes, who graduated from Columbia University in 1913, has been associated with the Illinois company since 1925 when he became equipment and building engineer. In 1928 he was appointed chief engineer and in 1938 general manager of the state area.

H. M. Archer (A '38) formerly research engineer, Detroit (Mich.) Edison Company,

has been appointed director of research of the Detroit Research Laboratories, Inc., recently organized to do independent general physical research. Mr. Archer has been with the Detroit Edison Company since he received the degree of bachelor of electrical engineering from Rensselaer Polytechnic Institute in 1937. He is the author of articles on infrared drying.

L. K. Sillcox (M '19, F '31) first vice-president, New York Air Brake Company, Watertown, recently was awarded the Medal of the American Society of Mechanical Engineers for "his pre-eminent and permanent contributions to the art and science of engineering, of transportation, of education, and the fine art of living. And for his ingratiating, inspiring influence upon the lives of men."

E. O. Jones (A '29, M '35) formerly sales engineer, industrial department, General Electric Company, Schenectady, N. Y., has joined the engineering staff of the Cook Electric Company and will head its new eastern division office at Greenwich, Conn. A graduate of Massachusetts Institute of Technology, Mr. Jones had been employed by the General Electric Company since 1926.

R. C. Griffith (A '35) formerly director of research, Heald Machine Company, Worcester, Mass., has been appointed manager of engineering and research, Denison Engineering Company, Columbus, Ohio. Prior to joining the Heald company in 1939 as director of research, Mr. Griffith had been employed by the General Electric Company Schenectady, N. Y., since 1926.

Francis Blossom (A '02, M '13) partner, Sanderson and Porter, consulting engineers, New York, N. Y., recently was made an honorary member of the American Society of Mechanical Engineers, "because of his many years of engineering and war usefulness; his courage, sound judgment, and high standards; engineers nationwide, hold him in the highest esteem."

T. D. Graybeal (A '38) instructor in electrical engineering at the University of California, Berkeley, was reported incorrectly to be a graduate of the University of California in the July issue of *Electrical Engineering*. Mr. Graybeal received the degree of bachelor of science from the University of Colorado in 1937.

OBITUARY • • • •

B. E. Sunny (A '03, F '13) retired, died October 5, 1943, in Chicago, Ill. Born May 22, 1856, in Brooklyn, N. Y., Mr. Sunny commenced his career as a telegraph operator for the Atlantic and Pacific Telegraph Company, Chicago, Ill., in 1873 and in 1876 became a telegraph manager. In 1879 he became superintendent for the Chicago Telephone Company and in 1888 president of the Chicago Arc Light and Power Company. In 1890 as western manager he joined the Thomson-Houston Electric Company which became the General Electric Company in 1892. Mr. Sunny remained with the latter company as western manager and vice-president until 1908 when he returned to the telephone industry as president of the Chicago Telephone Company. In 1922 he was elected chairman of the board. In addition he held the positions of president of the Wisconsin Telephone Company from 1911 to 1922 and chairman of the board from 1922 to 1930, as well as president of the Central Union, Michigan State, and Cleveland (Ohio) Telephone Companies from 1911 to 1922 and chairman of the board from 1922 to 1930. He was director and member of the executive committees of many electrical, railway, and utility companies for many years. As trustee, director, and committeeman, he served various organizations devoted to juvenile welfare and was also a member of the Western Society of Engineers, the Art Institute of Chicago, the Field Museum of Natural History, the Chicago Historical Society, and the executive committee of the American Red Cross.

Frederick Darlington (A '02, F '13) retired consulting engineer, Great Barrington, Mass., died October 27, 1943. Born at Lincoln University, Pa., April 23, 1867, he received the degree of bachelor of science in chemistry and physics from Pennsylvania State College in 1886. He commenced his career with the Phoenix Bridge and Iron Works, Phoenixville, Pa., in 1887. As a chemist he was employed by the Pittsburgh (Pa.) Testing Laboratory in 1888 and by the Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa., from 1890 to 1892. From 1892 to 1898 he worked as engineer for the United Electric Light and Power Company and the Brush Electric Light Illuminating Company in New York, N. Y. He was associated with the Stanley Instrument Company, Great Barrington, Mass., as vice-president and general manager, and with the Construction Company of America as chief engineer from 1898 to 1903. In 1905 he returned to the Westinghouse company as consulting engineer. During 1913 he served as vice-president and manager of the Alabama Power Company and consulting engineer for Sperling and Company, London, England. From 1914 until his retirement in 1936 he was associated with the Westinghouse company in a consulting capacity. During 1918 he served as consulting engineer to the power section of the War Industries Board.

Leonard Lord Elden (A '03, M '13) retired, died October 21, 1943, Wareham, Mass. Born in Buxton, Me., May 16, 1868, he received the honorary degree of doctor of science from Tufts College in 1928. Doctor Elden commenced his career as dynamo tender for the Merchants Electric Light and Power Company, Boston, Mass., in 1885. From 1888 to 1902 he was employed as an electrician by the Boston Electric Light Company. When the latter company became part of the Edison Electric Illuminating Company, he was appointed chief electrical engineer and he continued as technical adviser to that company until his retirement in 1932. He is credited with invention of several types of circuit breakers as well as other electrical equipment. His AIEE activities included membership on the protective devices committee, 1916–18; the committee on transmission and distribution and its successor, the committee on power transmission and distribution, 1922–32; the committee on safety codes, 1927–30. He was a representative of the National Electric Light Association on the electrotechnical commission of the World Power Conference and also a member of the National Fire Protection Association and the American Standards Association.

F. W. Brockhoff (A '38) safety engineer, Union Electric Company of Missouri, St. Louis, died recently. Mr. Brockhoff was born September 1, 1886, in St. Paul, Minn. His first position in the electrical field was apprentice wireman from 1903 to 1906 for Charles J. Sutter, Electrical Contractor. In 1906 he became apprentice machinist for the Emerson Electric Company and in 1908 for William Wurdach Electric Manufacturing Company. From 1909 to 1916 he served as electrical inspector in the central department of the Quartermaster Corps of the United States Government. He joined the service and sales department of the Mancha Storage Battery Locomotive Company in 1916 and in 1921 the farm lighting division of the Western Electric Company. In 1924 he entered the employ of the Union Electric Light and Power Company, St. Louis, to do electrical maintenance work. He was made relay tester in 1928 and foreman in 1930. In 1935 when the company name was changed to the Union Electric Company of Missouri, he was named general foreman in the substation division. In 1942 he was appointed safety engineer.

Francis Clifford Feiring (M '43) electrical contractor, Plainfield, N. J., died September 19, 1943. He was born in Plainfield in 1898. In 1919 he entered the employ of the Aero Alarm Company, New York, N. Y., as traveling inspector of fire alarm installations, and in 1921 was made district superintendent. From 1924 to 1931 he was employed as inspector, sales engineer, estimator, and supervisor. In the latter position maintenance of all fire alarm and telephone installations were under his jurisdiction. During 1931 and part of 1932 he worked as subcontractor for Joseph Harper and Son Company, New York. In 1932 he formed the Bruce Engineering

Company, New York, which specialized in marine electrical work. Later he formed the Bruce Electric Company, Inc., which was dissolved in 1941. He was a partner in the Brelco Radio Company, and president of the Brelco Radio Corporation and the Allied Marine Contractors Association. Mr. Feiring was also an associate member of the Society of Naval Architects and Marine Engineers.

John Guistwite Miller (A '41) senior electrical engineer, Corps of Engineers, United States War Department, died in December 1942. He was born July 28, 1910, in Columbia, Pa., and was graduated from Cooper Union Institute in 1928. From 1928 to 1933 he worked as chief electrician and electrical engineer for the Morgan Laundries, Inc., New York, N. Y. As chief engineer for Camp Milford, Inc., New York, from 1933 to 1937, he had charge of building a vacation resort. As electrical engineer he joined the Davidson Electric Company, Brooklyn, N. Y., in 1937 and in 1938 the A. C. Electric Company, Columbia, Pa. In 1940 he entered the employ of the War Department and as chief electrical engineer for the Zone Constructing Quartermaster Army Base, Boston, Mass., had charge of all the electrical work done for the Army in the New England States. In 1941 he was transferred to Omaha, Nebr., as senior electrical engineer, subsequently going to Australia from which he returned in 1942.

Herman A. Tepel (A '23) secretary of the Adalet Manufacturing Company, Cleveland, Ohio, died July 13, 1943. Mr. Tepel was born in Dusseldorf, Germany, July 31, 1882. He commenced his career as an apprentice construction electrician in Pittsburgh, Pa., in 1899 and in 1907 was superintendent of construction for the L. A. Comstock Company, Pittsburgh, Pa. From 1908 to 1910 he worked as electrical contractor in Louisville, Ky. He was connected with the Marine Electric Company, Louisville, as vice-president, estimator, and engineer from 1911 to 1914. During 1915 he was superintendent of construction for the F. A. Clegg Company, Louisville. From 1916 to 1920 he was superintendent of construction for the Cuyahoga Power Construction Company, Cleveland, Ohio. In 1920 he joined the Dingle-Clark Company, Cleveland, as superintendent of construction and estimator and in 1925 became chief engineer of the company. He was made secretary of the Adalet company in 1937.

John Willis Hines (A '22) engineer, operation and engineering department, American Telephone and Telegraph Company, New York, N. Y., died September 30, 1943. Born in Riverton, Conn., December 12, 1891, he was graduated from Massachusetts Institute of Technology with the degree of bachelor of science in electrical engineering in 1914. He commenced his association with the American Telephone company in the outside plant department in 1914. From 1917 to 1919 he served in the United States Navy. In 1919 he returned to the engineering department of the American Telephone company where he was engaged in preparing specifications for outside construction. Since 1929 he had been a member of the foreign wire relations section and had worked with the power industry on co-ordination problems. Recently he participated in the preparation of Part II of the fifth edition of the National Electrical Safety Code as a member of several of the technical subcommittees, the committee preparing the discussion, and the interpretations committee.

J. H. Roddey (A '06, M '13) superintendent of operation, Duke Power Company, Charlotte, N. C., died October 18, 1943. Mr. Roddey, who was born January 8, 1881, in Rock Hill, S. C., was graduated from Clemson Agricultural and Mechanical College with the degree of bachelor of science in electrical engineering in 1901. After working in the testing department of the General Electric Company, Schenectady, N. Y., from 1902 to 1906, he joined the Southern Power Company, Charlotte, which later was consolidated with the Duke Power Company. He assumed the duties of superintendent of operation in 1907, the year that position was created.

Clifton V. Edwards (A '99) lawyer and member of the firm, Edwards, Bauer, and Pool, New York, N. Y., died October 2, 1943. Mr. Edwards, who was born in Washington, D. C., May 21, 1872, attended the Columbian Law School and the law school of New York University. He specialized in patent law and since 1894 had been engaged in prosecuting applications for patents, patent litigation, and general legal business relating to patents in New York State. He was admitted to practice in the Federal courts and, in 1893, to the bar in Washington, D. C., as well.

Hervey D. Plenge (A '24) electrical engineer, General Electric Company, Schenectady, N. Y., died June 12, 1943. Born in Charleston, S. C., January 12, 1890, he was graduated from Clemson Agricultural and Mechanical College with the degree of bachelor of science in electrical engineering in 1910. From 1910 to 1912 he was employed in the testing department of the General Electric Company. In 1912 he was transferred to the a-c turbine engineering department.

John L. Bauerle (A '37) engineering assistant, Cleveland (Ohio) Electric Illuminating Company, died September 25, 1943. Born November 25, 1907, in South Fork, Pa., he received the degree of bachelor of science in electrical engineering from Carnegie Institute of Technology in 1929. That year he joined the Cleveland Electric company where he worked on substation design.

MEMBERSHIP ● ●

Recommended for Transfer

The board of examiners, at its meeting on November 18, 1943, recommended the following members for transfer to the grade of membership indicated. Any objection to these transfers should be filed at once with the national secretary.

To Grade of Fellow

Abbott, A. L., engr., National Electrical Manufacturers Association, New York.
Bennion, H. S., vice-president & managing director, Edison Electric Institute, New York.
Manson, R. H., vice-president & General Mgr., Stromberg-Carlson Co., Rochester, N. Y.
Walton, P. J., application engr., General Elec. Co., Philadelphia, Pa.
Whitaker, W. G. H., chief engr., New Jersey Bell Telephone Co., Newark, N. J.

5 to grade of Fellow

To Grade of Member

Bacon, F. S., asst. mgr., Central Station Div., Westinghouse Elec. & Mfg. Co., Boston, Mass.
Bankus, J., chief engr., Portland General Elec. Co. Portland, Oreg.
Bartles, S. P., asst. elec. engr., Eastman Kodak Co. Rochester, N. Y.
Brannin, R. S., project engr., Sperry Gyroscope Co. Inc., Garden City, N. Y.
Brown, G. O., mgr. of distribution, Kansas City Powe & Light Co., Kansas City, Mo.
Buckley, J. L., asst. elec. engr., Pacific Gas & Elec Co., San Francisco, Calif.
Chambers, M. A., supt. of telephone & telegraph Standard Oil Co. of Louisiana, Shreveport, La.
Christensen, P. M., chief engr., Colt's Patent Fir Arms Mfg. Co., Hartford, Conn.
Clark, J. H., plant staff asst., Southern Calif. Tel. Co Los Angeles, Calif.
Frey, A. P., asst. supt. of equipment, Baltimore Transi Co., Baltimore, Md.
Glentzer, K. V., engr., Illinois Bell Telephone Co. Chicago, Ill.
Hagenguth, J. H., in charge of high voltage engg. lab. General Elec. Co., Pittsfield, Mass.
Hansell, C. W., research supervisor in charge of Rock Point Lab., RCA, Rocky Point, N. Y.
Hemphill, L. F., section engr., General Elec. Co. Fort Wayne, Ind.
Irwin, J. H., mgr., Atlanta Office, Aluminum Company of America, Atlanta, Ga.
Matzinger, J. R., design engr., Goodyear Aircra Corp., Akron, Ohio.
Mullin, L. A., asst. prof. of elec. engg., Syracuse University, Syracuse, N. Y.
Onory, G. S., elec. engr., United States Maritime Comm., Oakland, Calif.
Peck, R. R., field engr., Raytheon Mfg. Co., Waltham Mass.
Rips, J. L., elec. engr., Consolidated Vultee Aircraft Corp., San Diego, Calif.
Ruddell, L. G., elec. engr., Kansas City Power & Light Co., Kansas City, Mo.
Santiago, F., elec. engr., Wellman Engg. Co., Akron, Ohio.
Simmons, H., asst. supt. elec. dept., Pacific Naval Air Bases, Port Hueneme, Calif.
Smith, J. H., asst. prof. of elec. engg., Cornell University, Ithaca, N. Y.
Taylor, P. L., general engg., Allis-Chalmers Mfg. Co., Boston, Mass.
Vonasch, R. W., asst. mgr., Federal and Marine Div., Ward Leonard Elec. Co., Mt. Vernon, N. Y.
Weil, R. T., asst. prof. of elec. engg., Manhattan College, New York.
Wolf, Harold, asst. prof. of elec. engg., College of the City of New York, New York.

28 to grade of Member

Applications for Election

Applications have been received at headquarters from the following candidates for election to membership in the Institute. Names of applicants in the United States and Canada are arranged by geographical District. Any member objecting to the election of any of these candidates should so inform the national secretary before December 31, 1943, or February 31, 1944, if the applicant resides outside of the United States or Canada.

To Grade of Member

Agnew, E. A., Goodyear Tire & Rubber Co., Akron., Ohio.
Baer, W. O. (Re-election), Cutler-Hammer, Inc., Milwaukee, Wis.
Bartlett, C. A. (Re-election), Amer. Tel. & Tel. Co., Chicago, Ill.
Brims, G. J., Int. Tel. & Tel., New York, N. Y.
Brous, D. W., Anchor Mfg. Co., Manchester, N. H.

Chadwich, G. L. (Re-election), Lord Elec. Co., Inc., Boston, Mass.
Clarke, C. A. (Re-election), Federal Tel. & Radio Corp., East Newark, N. J.
Clos, C., New York Tel. Co., New York, N. Y.
Colburn, F., Northampton Polytechnic, London, England.
Crocker, T. D., Northern States Pr. Co., Minneapolis, Minn.
Cummins, H. C., Northern States Pr. Co., Minneapolis, Minn.
Das Amrendra, C. (Re-election), Rajkot State Elec. S. Co., Rajkot, India.
Duerr, K. B., Western Union Tel. Co., New York, N. Y.
Eddie, J., George G. Sharp, Cons. Engr., Oakland, Calif.
Fronmuller, T. C. (Re-election), Pacific Gas & Elec. Co., San Francisco, Calif.
Gieb, H. B., Consulting & Design Engr., Dallas, Tex.
Green, A. P., A. P. Green Fire Brick Co., Mexico, Mo.
Haggart, J. D., British Columbia Elec. Ry. Co., Vancouver, B. C., Can.
Hastie, J. S., Scottish Cables, Ltd., Renfrew, Scotland.
Hayman, R. E., Rural Elec. Adm., Oklahoma City, Okla.
Hoffmann, W. W. (Re-election), Gen. Elec. X-Ray Corp., Chicago, Ill.
Hoover, R., Western Union Tel. Co., New York, N. Y.
Hudson, H. H. (Re-election), Elec. Storage Battery Co., New York, N. Y.
Jones, J. R., Douglas Aircraft Co., Inc., Los Angeles, Calif.
Kearns, J. L., Federal Public Housing Authority, Kansas City, Mo.
Kerr, C. L. (Re-election), Gen. Elec. Co., Denver, Colo.
Matz, C. L. (Re-election), Commonwealth Edison Co., Chicago, Ill.
Menanteaux, A. R., Bell Tel. Labs., Inc., New York, N. Y.
Miller, J. R., Bendix Aviation Corp., Brooklyn, N. Y.
Moyer, L. M. (Re-election), General Elec. Co., Portland, Oreg.
Peckham, J. W., Bristol Co., Waterbury, Conn.
Rendel, G. H., Carnegie-Illinois Steel Corp., Pittsburgh, Pa.
Russler, F., American Tel & Tel. Co., Chicago, Ill.
Straiton, A W., University of Texas, Austin, Texas.
Villeneuve, J. A., Marine Industries, Ltd., Sorel, Que., Can.
West, H. E., Amer. Tel. & Tel. Co., Philadelphia, Pa.
Zeitlin, A., Rogers Diesel & Aircraft Corp., New York, N. Y.

37 to grade of Member

To Grade of Associate

United States and Canada

1. North Eastern

Barrett, S. R. (Re-election), Cobble Mountain Power Station, Westfield, Mass.
Borders, C. R., Harvard University, Cambridge, Mass.
Emerson, H. J., Eastman Kodak Co., Rochester, N. Y.
Gallagher, J. J., Jr., General Elec. Co., Bridgeport, Conn.
Gardiner, C. B., Central N. Y. Power Corp., Potsdam, N. Y.
Gehringer, P. J., Gen. Elec. Co., Schenectady, N. Y.
Millar, N. P., Gen. Elec. Co., West Lynn, Mass.
Ransom, M. E., Jr., Graybar Elec. Co., Binghamton, N. Y.
Saretzky, S., Holtzer & Cabot Elec. Co., Boston, Mass.
Seibert, W. H., Western Union Tel. Co., Syracuse, N. Y.
Spitzer, C. P., Yale University, New Haven, Conn.

2. Middle Eastern

Blatchford, J. W., Southern Wood Preserving Co., Philadelphia, Pa.
Bragg, S. J., Mack Mfg. Corp., Allentown, Pa.
Cochener, R. L., Gen. Elec. Co., Philadelphia, Pa.
Day, F. H., National Bureau of Standards, Washington, D. C.
Enion, R. A., Bell Tel. Co. of Penna., Philadelphia, Pa.
Farmer, M. H., Ohio Edison Co., Akron, Ohio.
Garrison, R. E., Jr., Naval Research Lab., Washington, D. C.
Hartzell, R. C. (Re-election), Cinn. Gas & Elec. Co., Cincinnati, Ohio.
Janos, A. G., General Elec. Co., Erie, Pa.
Jennings, L. D., Westinghouse E. & M. Co., Sharon, Pa.
Johnson, G. E., U. S. Dept. of Commerce, Washington, D. C.
Jones, S., Heinemann Circuit Breaker Co., Trenton, N. J.
Locks, A. J., Westinghouse Elec. Int'l. Co., Washington, D. C.
McKinney, M., Locke Insulator Corp., Cincinnati, Ohio.
Musselman, F. L., Gen. Elec. Co., Philadelphia, Pa.
Pask, D. A., Lieut., RNVR, Washington, D. C.
Polich, A. L., Johns Hopkins Univ., Silver Spring, Md.
Rasmusen, R. P. C., Babcock & Wilcox Co., Barberton, Ohio.
Sarson, (Miss) K. O., U. S. Navy Dept., Camden, N. J.
Schneeberger, R. J., Westinghouse Elec. & Mfg. Co., East Pittsburgh, Pa.
Walley, O. C., Westinghouse Elec. & Mfg. Co., Lima, Ohio.

3. New York City

Aichele, P. F., Sperry Gyroscope Co., Inc., Brooklyn, N. Y.
Armiento, L., Ward Leonard Elec. Co., New York, N. Y.
Berry, C. D., Nat. Union Radio Corp., Newark, N. J.
Binkowski, E. W., West. Elec. Elevator Co., Jersey City, N. J.
Craine, R. B., James Stewart Associates, New York, N. Y.
Cronenberg, A. W., Todd Shipyards Corp., Hoboken, N. J.
Genodman, M., Todd-Hoboken Shipyard, Hoboken, N. J.
Goodwin, R. F., Monitor Controller Co., New York, N. Y.
Hackett, J. W. (Re-election), The Okonite Co., New York, N. Y.
Hillig, K. F., S. J. O'Brien, Inc., New York, N. Y.
Johnson, H. M., Baker & Co., Newark, N. J.
Jurick, E. F., Radio Television Institute, New York, N. Y.
Lowe, G. G. (Re-election), The Peelle Co., Brooklyn, N. Y.
Mageoch, N. H., Western Elec. Co., Kearny, N. J.
Mayerbach, A. H., S. J. O'Brien, Inc., New York, N. Y.
Price, R. D., U. S. War Dept., Newark, N. J.
Repose, F., Radio Television Institute, New York, N. Y.
Ridley, B. W., U. S. Navy Yard, Brooklyn, N. Y.
Rioux, R. P., Long Island Lighting Co., Mineola, N. Y.
Shalhoub, W. J., Automatic Paper Mach. Co., Hoboken, N. J.
Singalewitch, P., Westinghouse E. & M. Co., Newark, N. J.
Suan, S. Y., Nat. Resources Comm. of China, New York, N. Y.
Teetsel, H., Todd Shipyards Corp., Weehawken, N. J.
Vanina, R. A., Electric Boat Co., Bayonne, N. J.
Wahlstrom, F. A., Lieut., U. S. Army, Red Bank, N. J.

4. Southern

Armstrong, R. W. (Re-election), Chas. T. Main, Inc., Kingsport, Tenn.
Ball, D. J. (Re-election), Newport News Shipbldg. & Dry Dock Co., Newport News, Va.
Bossert, H. F., Signal Corps, U. S. Army, Arlington, Va.
Butterworth, L. A., J. A. Jones Constr. Co., Inc., Brunswick, Ga.
Carson, B. L., Amer. Cast Iron Pipe Co., Birmingham, Ala.
Edwards, J. P., Duke University, Durham, N. C.
Emmons, W. M. (Re-election), Westinghouse Elec. & Mfg., Atlanta, Ga.
Goss, C. G., Louisiana Polytechnic Inst., Ruston, La.
Hanchey, W. R., Carolina Pr. & Lt. Co., Raleigh, N. C.
Keller, D. F., Duke University, Durham, N. C.
Maher, L., Ensign, U. S. Navy Yard, Charleston, S. C.
Palmer, J. E., Jr. (Re-election), U. S. Navy, Norfolk, Va.
Rudesill, R., Tennessee Valley Authority, Wilson Dam, Ala.

5. Great Lakes

Anderson, C. J., Barber Colman Co., Rockford, Ill.
Becker, H. I., Jr., General Elec. Co., Fort Wayne, Ind.
Christian, T. R., Cutler-Hammer, Inc., Milwaukee, Wis.
Cogger, C. S., C. M. Hall Lamp Co., Detroit, Mich.
Doyen, J. E., U. S. Maritime Comm., Chicago, Ill.
Ecker, H. W., Minn. Min. & Mfg. Co., St. Paul, Minn.
Hackett, H. E., Gen. Elec. Co., Fort Wayne, Ind.
Holt, D. L., Gen. Motors Corp., Anderson, Ind.
Hopkins, T. A., Caterpillar Tractor Co., Peoria, Ill.
Hutchinson, G., Square D Co., Milwaukee, Wis.
Markese, J., Delta-Star Elec. Co., Chicago, Ill.
Markese, S. D., Delta-Star Elec. Co., Chicago, Ill.
Moore, S. A. (Re-election), Prime Mfg. Co., Milwaukee, Wis.
Osterberg, R. M., James Stewart & Co., Chicago, Ill.
Powers, W. R., Gen. Elec. Co., Fort Wayne, Ind.
Ritchie, E. A., Kurz & Root Co., Appleton, Wis.
Steele, H. P., Benjamin Elec. & Mfg. Co., Des Plaines, Ill.
Welch, W. E. (Re-election), Minn. Honeywell Regulator Co., Minneapolis, Minn.
Young, C. G., Gen. Elec. Co., Fort Wayne, Ind.

6. North Central

Schmidt, A. W., Bureau of Reclamation, Denver, Colo.

7. South West

Armstrong, J. F., El Paso Elec. Co., El Paso, Texas
Crosthwait, G. N., Texas Elec. Serv. Co., Wichita Falls, Texas.
DePrato, E. W., General Elec. Co., Houston, Texas.
Gillam, C. E., Kansas City Pr. & Lt. Co., Kansas City, Mo.
Glomski, W. E., S. W. Bell Tel. Co., St. Louis, Mo.
Green, J. D., Boeing Airplane Co., Wichita, Kans.
Houser, K. O. (Re-election), Kansas Gas & Elec. Co., Wichita, Kans.
Johnson, L., Amer. Tel. & Tel. Co., St. Louis, Mo.
Joyce, S. F. (Re-election), Union Elec. Co. of Mo., St. Louis, Mo.
Koenig, W. J., Amer. Tel. & Tel. Co., St. Louis, Mo.
Lyons, E. F., WKY Radiophone, Inc., Oklahoma City, Okla.
Martin, K. H., Kansas State College, Manhattan, Kans.
Morris, R. E., Rural Elec. Adm., St. Louis, Mo.
Phillips, A., Allis-Chalmers Mfg. Co., Beaumont, Texas.
Phillips, F. L. (Re-election), Kansas City Pr. & Lt. Co., Kansas City, Mo.
Phillips, W. R., Moloney Elec. Co., St. Louis, Mo.
Powell, L. E., Joslyn Southwest Co., Dallas, Texas.
Robbins, R. B., Penn. Shipyards, Inc., Beaumont, Texas.
Stubbins, W. F., Capt., U. S. Army, San Antonio, Texas.
Turner, J. C., S. W. Bell Tel. Co., Oklahoma City, Okla.
Wanko, A. N., Moloney Elec. Co., St. Louis, Mo.
Warner, W. C., Penn. Shipyards, Inc., Beaumont, Texas.
White, R. E. (Re-election), Gulf States Utilities Co., Beaumont, Texas.
Wuetherich, R. A., Union Elec. Co. of Mo., St. Louis, Mo.

8. Pacific

Adams, P. H., San Diego Gas & Elec. Co., San Diego, Calif.
Beach, W. J., 2 Chapel St., Placerville, Calif.
Bertolet, E. C., 1st Lt., U. S. Army, San Bernardino, Calif.
Braud, H. M., U. S. War Dept., Presidio of San Francisco, Calif.
Churchill, P. K., Line Material Co., San Francisco, Calif.
Day, C. R., Modesto Irrigation District, Modesto, Calif.
Dimmick, M. A., Richmond Shipyard #3, Richmond, Calif.
Domino, F. E., U. S. Bureau of Reclamation, Phoenix, Ariz.
Ferrara, P. D., Vultee Aircraft, Inc., Vultee Field, Calif.
Gaylord, J. M., Byron Jackson Pump Co., Los Angeles, Calif.
Irish, F. M. (Re-election), Central Arizona Lt. & Pr. Co., Phoenix, Ariz.
King, F. D., Consolidated Vultee Aircraft Corp., Downey, Calif.
King, G. C., Consolidated Vultee Aircraft Corp., Downey, Calif.
Koepke, W. E., Kaiser Shipyard, Richmond, Calif.
Kremer, I. A., Consolidated Vultee Aircraft Corp., Vultee Field, Calif.
Murray, B. A., Electric Supplies Dist. Co., San Diego, Calif.
Navin, J. F., U. S. Navy, Vernon, Calif.
Richardson, J. W., Consolidated Vultee Aircraft Corp., Vultee Field, Calif.
Rude, M., Ensign, USNR, San Francisco, Calif.
Thompson, I. F., Consolidated Vultee Aircraft Corp., Vultee Field, Calif.
Vinoff, I., Western Pipe & Steel Co., So. San Francisco, Calif.
Watson, J. C., U. S. Maritime Comm., Oakland, Calif.

9. North West

Ley, H. T., Westinghouse Elec. & Mfg. Co., Portland, Oreg.
Porter, W., Aluminum Co. of America, Troutdale, Oreg.
Volpe, J. S. (Re-election), Portland Gen. Elec. Co. Portland, Oreg.

10. Canada

Edmonds, A., Swansea Hydro-Elec. System, Swansea, Ont., Can.
Irwin, G. J., Philco. Corp. of Canada, Toronto, Ont., Can.

Elsewhere

Aguila, R. M., Elec. Contractor, Coldel Valle, D. F., Mex.
Brun, O. F. (Re-election), Allis-Chalmers de Mexico, Mexico, D. F., Mex.
Garcia, J. C., Productores De Energia Elec. "ElSalto" La Piedad, Mich., Mex.
Luna Fernando, O., Dir. del Catastro Depto del D. F. Mexico, D. F., Mex.
Mendoza Leonardo, V., Cia. West. Elec. Int'l. Co., Mexico City, Mex.
Molina, U. C., Amacuzac Hydraulic Elec. Co., Mexico City, Mex.
Perez, J. G., Engineer, Mexico, D. F., Mex.
Querejeta, A., Cia. Hulera Euzkadi & Goodrich, Mexico City, Mex.
Valdovinos, R. L., Elec. Contr., San Juan, P. R.

Total to Grade of Associate
United States and Canada, 140
Elsewhere, 9

OF CURRENT INTEREST

ECPD Forges Ahead

Despite Wartime Difficulties

Although wartime conditions have influenced many of the activities of the Engineers' Council for Professional Development during the past year, nevertheless progress in furthering the aims of ECPD has been achieved, and attention has been given to problems arising directly as a result of the war. These were among the more important facts brought out at the 11th annual meeting of ECPD held October 23, 1943, in New York, N. Y. Joint organization of eight national engineering societies, ECPD has as its general objective the enhancement of the professional status of the engineer.

Completion of plans for pursuing ECPD activities at "the level of the individual" was one of the most important accomplishments of the year. At the 1942 annual meeting (*EE, Dec. '42, pp. 626–30*) the council approved a plan of action proposed by AIEE representative J. F. Fairman (F '35), and, during the past year, the plan has been approved by the boards of the constituent societies. At the 1943 meeting, publication of a booklet, prepared by Mr. Fairman, outlining the plan and how to put it into action, was authorized. Details are reported elsewhere in this issue.

Among other important actions was approval of a statement of the "Faith of the Engineer," prepared by the ECPD committee on ethics headed by AIEE Past President D. C. Jackson (F '12); full text of this statement is given in a separate item. Progress in the preparation of the previously authorized "Manual for Junior Engineers" was reported by Chairman E. S. Lee (F '30) of the ECPD committee on professional training. Mr. Lee announced that W. E. Wickenden (F '39) engineering educator and president of Case School of Applied Science, Cleveland, Ohio, has accepted authorship of the manual and now has it under preparation. Annual reports of the various committees of ECPD and of the representatives of the constituent societies were presented.

Most severely curtailed by wartime conditions of all ECPD activities during the past year was the curricula-accrediting program. Only two new courses, both in chemical engineering, were accredited at the 1943 annual meeting.

E. S. Lee, newly appointed AIEE representative on the council, was elected chairman for the year 1943–44. Other officers elected and committees appointed for the year are reported separately. Expenditures during the year 1942–43 were reported as $10,642.14, and a budget totalling $12,150.00 for 1943–44 was adopted. ECPD income is derived chiefly from contributions from the constituent societies (AIEE $1,700 for 1943–44); for the past several years, these have augmented by an annual contribution from the Engineering Foundation which has allotted $5,000 for 1943–44.

Supplementing the morning and afternoon sessions of the 1943 annual meeting, a dinner meeting was held at the Engineers Club for the officers and members of the council and officers of the constituent societies. Following dinner was a round-table conference discussion of postwar engineering education.

Officers and Committees

ECPD Officers elected for 1943–44 are:

E. S. Lee (F '30) engineer, General Electric Company, Schenectady, N. Y., *chairman;* James W. Parker, vice-president and chief engineer, The Detroit Edison Company, Detroit, Mich., *vice-chairman;* S. L. Tyler, executive secretary, American Institute of Chemical Engineers, New York, N. Y., *secretary;* R. L. Sackett, assistant to secretary, American Society of Mechanical Engineers, New York, N. Y., *assistant secretary.*

Members of the executive committee are:

E. S. Lee (F '30) *chairman ex-officio;* J. W. Parker, *vice-chairman ex-officio;* George W. Burpee, consulting engineer, Coverdale and Colpitts Co., New York, N. Y.; W. B. Plank, professor of mining, Lafayette College, Easton, Pa.; R. L. Geotzenberger, vice-president, Minneapolis-Honeywell Regulator Co., Philadelphia, Pa.; J. F. Fairman (F '35) assistant vice-president, Consolidated Edison Company of New York, New York, N. Y.; J. B. Challies, vice-president and executive engineer, Shawinigan Water and Power Company, Montreal, Que., Can.; C. C. Williams, president, Lehigh University, Bethlehem, Pa.; S. D. Kirkpatrick, editor, *Chemical and Metallurgical Engineering,* New York, N. Y.; V. M. Palmer, superintendent, industrial engineering department, Kodak Park Works, Eastman Kodak Company, Rochester, N. Y.

The following representatives of ECPD member societies were announced for the 1943–46 term:

Reappointments—R. E. Bakenhus, rear admiral, United States Navy, retired; W. B. Plank, professor of mining, Lafayette College, Easton, Pa.

New appointments—R. L. Geotzenberger, vice-president, Minneapolis Honeywell-Regulator Company,

Faith of the Engineer

Approved by Engineers' Council for Professional Development at its annual meeting October 23, 1943, on recommendation of the ECPD committee on principles of engineering ethics

I am an engineer. In my profession I take deep pride, but without vainglory; to it I owe solemn obligations that I am eager to fulfill.

As an engineer, I will participate in none but honest enterprise. To him that has engaged my services, as employer or client, I will give the utmost of performance and fidelity.

When needed, my skill and knowledge shall be given without reservation for the public good. From special capacity springs the obligation to use it well in the service of humanity; and I accept the challenge that this implies.

Jealous of the high repute of my calling, I will strive to protect the interests and the good name of any engineer that I know to be deserving; but I will not shrink, should duty dictate, from disclosing the truth regarding anyone that, by unscrupulous act, has shown himself unworthy of the profession.

Since the Age of Stone, human progress has been conditioned by the genius of my professional forebears. By them have been rendered usable to mankind Nature's vast resources of material and energy. By them have been vitalized and turned to practical account the principles of science and the revelations of technology. Except for this heritage of accumulated experience, my efforts would be feeble. I dedicate myself to the dissemination of engineering knowledge, and, especially, to the instruction of younger members of my profession in all its arts and traditions.

To my fellows I pledge, in the same full measure I ask of them, integrity and fair dealing, tolerance and respect, and devotion to the standards and the dignity of our profession; with the consciousness, always, that our special expertness carries with it the obligation to serve the public and humanity with complete sincerity.

Philadelphia, Pa.; E. S. Lee (F '30) General Electric Company, Schenectady, N. Y.; George G. Brown, professor of chemical engineering, University of Michigan, Ann Arbor, Mich.; C. R. Young, dean, faculty of applied science and engineering, University of Toronto, Toronto, Ont., Canada; C. E. Lawall, president, West Virginia University, Morgantown, W. Va.

Newly elected committee chairmen for 1943–44 are:

Committee on professional training, John C. Arnell (A '28) manager, personnel co-ordination bureau, Consolidated Edison Co. of N. Y.

Committee on information, S. L. Tyler, secretary, American Institute of Chemical Engineers, New York, N. Y.

Th following committee chairmen were re-elected for the coming year:

Committee on student selection and guidance, A. R. Cullimore, president, Newark (N. J.) College of Engineering.

Committee on engineering schools, D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind.

Committee on professional recognition, C. F. Scott, professor emeritus of electrical engineering, Yale University, New Haven, Conn.

Report of Chairman

In presenting his annual report for 1942–43, Retiring Chairman R. E. Doherty (F '39) urged that three points of policy be observed carefully if ECPD is to continue to function effectively. He also reported on the contributions to the war effort made by the consultative committee on engineering man power, established under instigation of ECPD officers, and reviewed the accomplishments of the various ECPD committees.

"During my six years on the council and three as chairman," he said, "I have become keenly aware that if the council is to endure and accomplish the purposes for which it was organized, certain principles must be recognized; otherwise the centrifugal forces that have broken other co-operative enterprises in the engineering profession might also break Engineers' Council for Professional Development. It early became clear that we must remove unnecessary internal pressures, which were largely centered upon misunderstandings and matters of detail, and address the attention and energies of the council to its constructive business. In the second place, it became clear also that a distorted conception of purpose must be cleared up. The council's business is defined with reasonable clarity in the charter and is represented by the four standing committees. The growing tendency to regard the work of accrediting curricula as the primary, and perhaps the only significant, purpose was ill advised; the objectives represented by these four committees were *all of them* of great fundamental importance. Finally, and above all, it became clear that the council should stick to its business and not yield to the pressures which are constantly arising for it to interest itself in matters outside, or at the fringe of, its purview. If the council sticks to its purposes as now defined until it reasonably accomplishes them, it will avoid disruptive pressures and will also, I hope, have demonstrated to the constituent bodies that they may with assurance delegate other activities to it if the future should bring new problems that require joint action of a character which the council could appropriately handle. . . .

"Here, then, are three points: We must remove unnecessary internal pressures, not permit purpose to become distorted, and stick to our business. In retiring from the chairmanship this year, I strongly commend these points to the council's perennial consideration.

"Our work has gone forward. One result is improved machinery for doing business. It has seemed clear that neither an educational campaign nor other activities that must be pursued 'at the level of the individual' in order to be effective, could be carried out without the establishment of new machinery or rather the supplying of new links between parts of existing machinery. During the past two years there has been continued study of this problem and discussion of it from time to time in meetings of the executive committee At the last council meeting a plan drafted by J. F. Fairman, chairman of a special committee on this matter, had reached a stage where the council approved it in principle and authorized him to seek approval of the boards of the constituent organizations. I am happy to report that this carefully constructed plan has been approved by the constituent organizations, and thus the machinery is created for carrying forward our work at the level of the individual engineer and engineering student. . . .

WORK OF CONSULTATIVE COMMITTEE ON ENGINEERING MAN POWER

"The Council has concerned itself indirectly with the problem of professional engineering man power in the war effort," Doctor Doherty declared, in reporting the achievements of the consultative committee on engineering man power. "Since ECPD itself was not, by its charter, in position to deal effectively with this urgent problem without elaborate procedures of approval, the chairman approached it in a way to obtain prompt action, to keep the council's interest, but not to involve its direct responsibility. Last year I reported upon the establishment, under the initiative of the officers of ECPD and after consultation with the secretaries of engineering bodies concerned, of the consultative committee on engineering of the then division of professional and technical employment and training of the War Manpower Commission, of which E. C. Elliott was head. This committee was to be a body to which Doctor Elliott's division could turn for counsel in connection with problems of engineering man power in the war effort, or which could, on its own initiative submit recommendations on behalf of the engineering profession. The committee proceeded promptly to deal with Doctor Elliott, whose purview at that time encompassed substantially all matters of engineering man power in the war effort, and with whom the committee's recommendations had great weight.

"Then, before significant results were accomplished unfortunate changes occurred in the War Manpower Commission. After "employment" was taken out of the division referred to and after the later resignation of Doctor Elliott, and also in view of the fact that the several engineering societies were independently sending recommendations to government agencies concerning professional engineering man power, it was clear to the consultative committee that its status was confused and ineffective and that a clarification should be sought if the committee was to continue. The committee took action to this end on May 18 1943, and requested on the one side that the boards of the engineering societies concerned authorize the committee to make recommendations in their behalf with the understanding that no recommendation would be made that did not have the unanimous approval of the societies concerned; and on the other side, that the War Manpower Commission change the status of the committee so that it would report not to a division or bureau representing only one phase of the general problem of engineering man power in the war effort, but to an office whose purview would encompass any matters about which the committee was likely to have a recommendation.

"At long last I am now in position to report that on the engineering side the proposal has been approved by the boards of the AIEE, ASME, AIME, AIChE, IRE, and SPEE. The ASCE and SAE have not approved. On the side of the War Manpower Commission I am pleased to report that I received only this week a letter notifying me informally that agreement had been reached as to the status of the consultative committee on engineering and that it would report to the chairman of the WMC committee on professional and technical personnel and service (a committee to co-ordinate the work of the several agencies within the WMC in connection with these matters); also that I would receive formal notification within a few days from the office of the executive director of the WMC. Thus the machinery is established for effective communication of counsel from the engineering profession to the WMC in connection with problems of professional engineering man power in the war effort."

The remainder of Chairman Doherty's report reviewed and commented on the work of the ECPD committees during the year. Abstracts of the reports of the four standing committees and of the committees on ethics and on employment conditions for engineers (formerly unionism) follow.

ECPD Committee Reports

STUDENT SELECTION AND GUIDANCE

As in former years, reported Chairman A. R. Cullimore, president, Newark (N. J.) College of Engineering, the ECPD committee on student selection and guidance has concerned itself with two major projects: Counseling and guidance of high-school students, and application of educational measurement for the selection and guidance of beginning students.

The counseling and guidance of high-school students with respect to engineering,

which involves acquainting prospective college students with the nature, requirements, and responsibilities of the profession, has met with special difficulties this year because of the problems posed and questions raised by Selective Service and the various training programs of the Armed Forces. However, the subcommittee in charge of this project prepared and circulated to high schools throughout the United States and to other interested persons digests of material concerning the most significant developments (Selective Service directives, reports of American Council on Education, and the like). Distribution of the booklet, "Engineering as a Career," was continued.

The committee's second project involves the design of tests to determine the probability of a prospective student's completing satisfactorily an engineering curriculum and a plan to determine the validity of such tests. The project has developed into a co-operative program sponsored jointly by the Society for the Promotion of Engineering Education, the Carnegie Foundation for the advancement of Teaching, and ECPD. A battery of tests devised by Albert B. Crawford, department of personnel study, Yale University, New Haven, Conn., has been given to entering freshmen students in a group of representative institutions, and at the end of the freshman year grades were secured for 1,600 individuals who had taken the tests. The results are now being correlated and analyzed.

ENGINEERING SCHOOLS

Believing that engineering colleges cannot be fairly judged while operating under war conditions, the ECPD committee on engineering schools has curtailed its inspections of curricula during the past year, reported Chairman D. B. Prentice, president, Rose Polytechnic Institute, Terre Haute, Ind. A few inspections, however, were necessary in order to complete studies already begun or to prevent injustices to certain institutions. Curricula at eight colleges were investigated, and special consideration was given to 11 curricula at other institutions. The temporary extension of provisional accrediting of 34 curricula at 14 schools is recommended, because it seems to the committee that these colleges are maintaining standards as well as can be expected under present difficulties. This recommendation was approved by the council at the 1943 annual meeting.

In regard to the postwar accrediting program, the committee is continuing to give attention to plans for the reappraisal of all accredited curricula within a reasonable period after the cessation of hostilities. Consideration also is being given to the possible future need for a review of the present accrediting policy and procedure.

In spite of war responsibilities, the committee's subcommittee on technical institutes is continuing its study of technical training of post-high-school but less than professional-engineering level. In addition to courses of the EMSWT (engineering, management, and science war training) type, endowed technical institutes, junior colleges, industrial technical institutes, evening schools of degree-granting colleges, and correspondence courses are being studied.

The committee on engineering schools, Chairman Prentice concluded, is fully aware of the problems confronting technological schools now and the difficulties to be faced in the postwar years. It is taking every precaution it believes feasible to help engineering colleges to maintain and protect scholastic standards.

PROFESSIONAL TRAINING

The "Manual for Junior Engineers," publication of which was authorized last year, received first attention of the ECPD committee on professional training, declared Chairman E. S. Lee (F '30), engineer, General Electric Company, Schenectady, N. Y. Obtaining an author was the main objective of the committee. The committee is most happy to report that W. E. Wickenden has accepted the authorship of the manual and is now writing it.

To keep complete the "General Reading List for Junior Engineers," the junior committee has planned for evaluation of old and new material by having junior groups read the various available books and report on same.

PROFESSIONAL RECOGNITION

Initiation of a co-operative program with a newly appointed committee of the Society for the Promotion of Engineering Education was reported by Chairman C. F. Scott (HM '29), professor emeritus of electrical engineering, Yale University, New Haven, Conn., to be the year's notable achievement of the ECPD committee on professional recognition. Present co-operative effort of the two groups is directed toward determining the most effective use of "The Second Mile," by W. E. Wickenden, in promoting professional attitudes in young engineers. "The Second Mile" was first published in the journals of three of the constituent societies (*EE, May '42, pp. 242–7*); since then nearly 20.000 reprints have been distributed by ECPD, principally through societies and schools.

"How to help in promulgating professional attitudes is a baffling problem," the report concludes. "There is no simple rule or formula for creating an interest and an attitude that will be self-propelling in personal development. The student may resent condescending kindergarten advice from teachers or engineers in practice but be receptive to information and experience and counsel that shows what to do and why. There is need for judgment, ingenuity, tact, and skill, and for a wholesome and invigorating environment.

"Along the 'Second Mile' are thought-provoking paragraphs which invite detours of exploration; articles in the society journals give 'something to think about'; much news in the public press has an engineering background or implication—often to be read between the lines.

"From all this there may evolve among young engineers a common ideal and a common purpose supplementing a common method and a common attitude of mind, which may transcend technical differentiation and lead to a new professional consciousness imbued by the spirit of the engineer. Such should be the basis of a 'profession' into which 'recognition' will have a new significance. Present confusion is the present stage in the evolution of engineering as a profession. We plan for the future."

The report was prefaced by a historical outline of the evolution of ECPD, with particular reference to the contributions of the late Conrad Lauer (*EE, Oct. '43, p. 466*) one of the originators of ECPD and early chairman of the committee on professional recognition.

PRINCIPLES OF ENGINEERING ETHICS

Three of the constituent societies have approved, with modifications in phraseology and arrangement, the proposed canons of ethics formulated by the ECPD committee on principles of engineering ethics, reported Chairman D. C. Jackson (F '12) professor emeritus of electrical engineering, Massachusetts Institute of Technology, Cambridge, Mass. Two of the other societies have made reports which are somewhat interdestructive. Four societies have not yet reported their actions. In the meantime, one well-organized local society has adopted the canons for its own use.

The committee plans to examine and correlate all recommendations received from the constituent societies and to make a final report on the canons of ethics to the 1944 annual meeting of ECPD.

EMPLOYMENT CONDITIONS FOR ENGINEERS

V. T. Boughton, chairman of the ECPD committee on employment conditions for engineers (formerly unionism), newly appointed in March 1943, reported briefly for that committee. He said that some factual material on the union movement as it affects engineers had been assembled and is now in the committee's hands for study. His suggestion that the committee make recommendations for the guidance of the constituent societies on the basis of the factual data assembled was approved by the council.

Plans Completed for Local Advancement of ECPD Aims

Provisions for furthering the objectives of Engineers' Council for Professional Development at the "level of the individual" were completed at the recent 11th annual ECPD meeting, with authorization of the publication of a booklet entitled "Will You Help?" The booklet will outline how existing local organization units of the constituent societies can be brought together for joint action on ECPD objectives, under the general inspiration and guidance of ECPD's four standing committees.

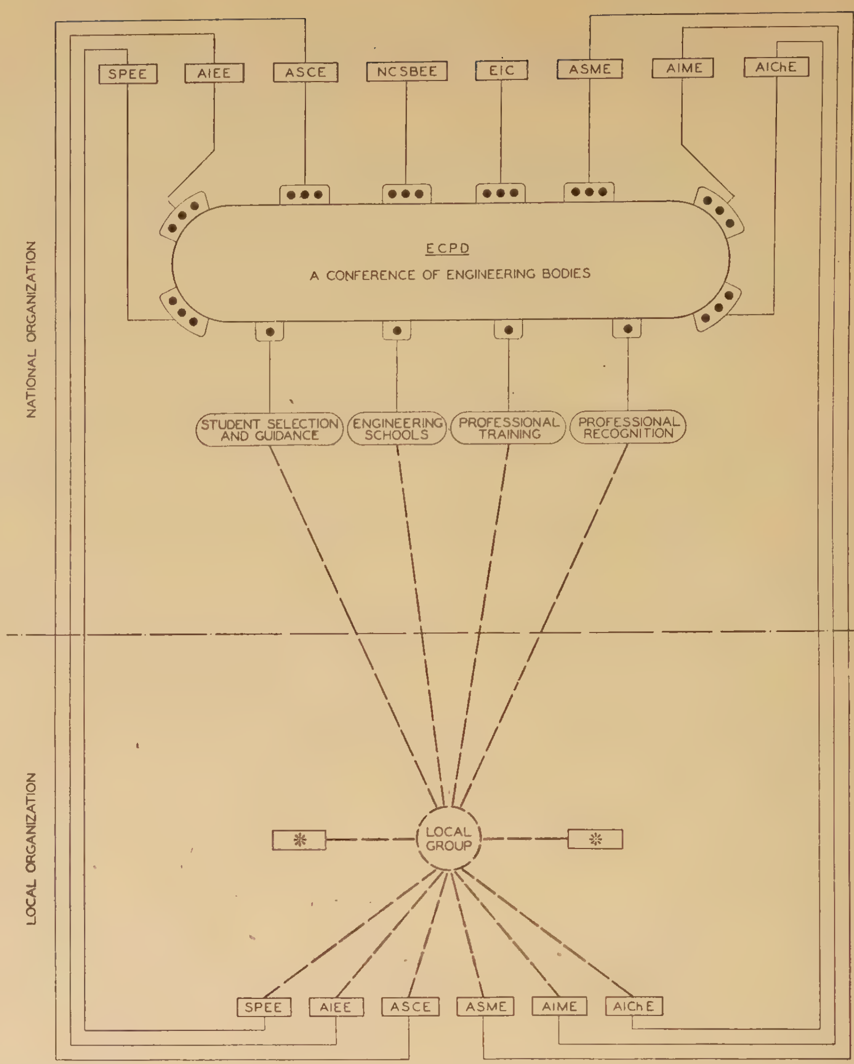
The plan was formulated and initially proposed by AIEE representative J. F. Fairman (F '35) assistant vice-president, Consolidated Edison Company of New York, N. Y. It was approved by ECPD at its 1942 annual meeting and has since been approved by the boards of the constituent

societies. The booklet describing the plan is expected to be issued soon and will be announced in *Electrical Engineering* as soon as available.

How the plan will function is illustrated by the accompanying diagram. Existing organizational channels are indicated by solid lines; new channels provided for by the new plan, by dashed lines. In almost every urban and industrial center and on every college campus there are terminal points of the existing channels in the form of local branches, chapters, or sections of the ECPD constituent societies.

The plan is that in such urban or collegiate centers joint groups be organized to work in the local field on the immediate objectives of ECPD under the general inspiration and guidance of ECPD's four standing committees. If the plan were carried out, the existing channels from each of the national headquarters of the participating bodies to the local units of those societies would be utilized more effectively for the flow both ways, between the council or its committees and the local joint group, of information and inspiration. It is a part of the plan that the local groups enter into direct correspondence with the standing committees of ECPD in order mutually to take advantage of the inspiration, enthusiasm, and experience of individuals working locally and nationally in corresponding fields. Thus, each local group would feel assured that it had the approval of the national body for active participation in the joint programs represented by ECPD. Thus, the approved policies and programs of the council and its committees could find effective national expression *at the level of the individual engineer.*

Suggestions as to where and how to begin will be included in the forthcoming booklet, but in general the best procedures for any specific locality will depend on local conditions, opportunities, and requirements. Committees of ECPD will gladly give assistance by answering questions, supplying information as to the experiences of other local groups, and making suggestions based on their observations. Inquiries may be addressed to Engineers' Council for Professional Development, 29 West 39th Street, New York 18, N. Y.



Outline diagram illustrating plan for establishing local joint groups for furthering ECPD objectives

This diagram relates only to conditions in the United States

—— *Existing organizations and lines of communication*

- - - *Proposed organizations and lines of communication*

* *Other local engineering societies*

Report to ECPD by AIEE Representatives

"Although the war has been a disturbing factor in every field of endeavor, yet if one can take the time to inquire, he frequently finds encouraging evidences of activity continuing in spite of that handicap," stated J. F. Fairman (F '35) in presenting the report of the AIEE representatives to the annual meeting of ECPD. "For example, in New York City the work on student guidance, sponsored by the metropolitan chapters of the participating bodies, is going on much as usual, even though an extra burden is imposed on the remaining counselors. Reports on this activity in other cities by delegates to last summer's national technical meeting of the AIEE confirm one's belief that the situation in New York City is not unique. This one inquiry and these reports are the sole basis for the optimistic generalization that the Institute has done, during the past year, as much as usual, or at least as much as could reasonably be expected under present conditions, to further the objectives of ECPD.

"In last year's report, mention was made of the groundwork laid for active encouragement of the Institute's local Sections to establish or to continue co-operative efforts with other local engineering groups in the fields of student selection and guidance and of professional training and of the fact that the next steps to be taken by the Institute would be in accord with the action taken by the council on the chairman's proposals for using the existing organization structures to fuller advantage. The Institute was the first of the participating bodies to approve the proposals and the plan set forth in the pamphlet entitled 'Will You Help?' and hopes that this potentially useful tool will shortly be available for distribution. [See item 'Plans Completed for Local Advancement of ECPD Aims.'] The Institute's representatives still believe, as they stated a year ago, that the greatest progress will be made by the participating bodies acting in concert on matters of this sort. The pamphlet will be tangible evidence of the determination of the participating bodies so to act. Renewed evidence of that determination is sorely needed.

"The 'Manual for Junior Engineers' which is in process of preparation will be another valuable aid in furthering the objectives of ECPD. This manual and similar aid should be made available for distribution and use at the earliest possible moment. It is suggested that consideration be given to the preparation of a suitable

manual or manuals for use in connection with that type of activity among students now being sponsored by the committee on professional recognition.

"If the council expects the local sections of the participating bodies to assist in the furtherance of the program, it must make available to them appropriate material on the subjects and definite suggestions as to how to do the job, and must be prepared to answer questions and supply encouragement. This will not be an easy task, but it will get the results for which the council was organized. In the opinion of this delegation, the council would wisely concentrate on its immediate objective for the present, rather than speculate about any broader objectives . . . ."

Organization of Radio Technical Planning Board and Panels Approved by Sponsors

Final details of over-all organization and selection of the technical panels to carry on the research upon which the recommendations of the Radio Technical Planning Board will be based have been completed by that board. As previously reported (*EE*, June '43, p. 278 and Sept. '43, p. 427) the RTPB will formulate plans, restricted to engineering considerations, for the technical future of the radio industry and services and advise the Government, industry, and the public of its recommendations.

The general plan of organization of RTPB submitted by the Radio Manufacturers' Association and the Institute of Radio Engineers to the initial sponsors was approved unanimously at a meeting September 15, and details of panel organization were decided at a meeting September 29. Initial sponsors of the Board are: American Institute of Electrical Engineers, American Institute of Physics, American Radio Relay League, FM Broadcasters, Inc., International Association of Chiefs of Police, National Association of Broadcasters, and National Independent Broadcasters. Other sponsors are expected to join RTPB later.

The Board as constituted is composed of one representative for each of its sponsors (nonprofit associations and societies with an important interest in radio which indicate willingness to co-operate in achieving the objectives of the RTPB). Organizations which are interested in the work of the board but do not wish to become sponsors can have representation on the panels or various committees reporting to the panels. Sponsors who contribute, in the first year of operation, a sum of $1,000 are designated contributing sponsors. AIEE is represented on this first board of six contributing sponsors by G. T. Harness (M '36) assistant professor of electrical engineering, Columbia University, New York, N. Y.

An administrative committee selected by the respective contributing sponsors has been made responsible for all RTPB expenditures, approval of the budget, and regulation of other fiscal matters. By means of a monthly report it will account to the sponsors for all expenditures. The chairman of RTPB will also be chairman of the administrative committee while other officers who may be required for effective functioning of the organization will be appointed by the administrative committee but will not have voting power in the decisions of the board. W. R. G. Baker (M '37) has been chosen as first chairman of the board.

The specific paths of investigation to be followed by the RTPB technical panels have been delineated. Each panel will embody its conclusions and recommendations, including all minority opinions, in a report which shall be submitted to the RTPB. Each sponsor's representative will inform his sponsor of the contents of the report to give the latter an opportunity to express its views on the report. If it so wishes, the sponsor may issue a statement to be released with the report. After consideration of the report and of the opinions of the sponsors, the RTPB may release the report with an accompanying statement prepared by the board or return it to the panel for further revision. A report may be returned to the panel only once and must be released on resubmission by the panel. Upon release, copies of each report and any accompanying statements will be transmitted on request to governmental agencies and industrial and professional organizations. Among the 13 panels which have been chosen and the scope of their work defined are:

1. Spectrum Utilization. For the analytical study of the factors pertinent to the most effective use of the transmission medium, A. N. Goldsmith (F '20) chairman.

2. Frequency Allocation. For studying the allocation of frequency bands to services on basis of propagation and equipment characteristics considering military requirements, public interest, and past practices, C. B. Jolliffe (M '34) chairman.

3. High-frequency generation for investigating the present status and probable progress in the development of electronic tubes and the necessary associated equipment for increasing frequency of generation and operation, R. M. Wise, chairman.

Other panels are devoted primarily to the review and development of standards in their respective fields. In this group are panels on: standard broadcasting, H. S. Frazier, chairman; very high-frequency broadcasting, G. E. Gustafson, chairman; television, D. B. Smith (A '35) chairman; facsimile, J. V. L. Hogan (M '30) chairman; radio communication, Haraden Pratt (F '37) chairman; relay system, E. W. Engstrom, chairman; radio range, direction, and recognition, W. P. Hilliard, chairman; aeronautical radio, J. C. Franklin, chairman; industrial, scientific, and medical equipment, C. V. Aggers (A '39) chairman; police emergency services, D. E. Noble (A '32) chairman.

L. C. F. Horle (F '35) manager of the Radio Manufacturers' Association data bureau has been chosen as co-ordinator of the panel organization and work. When the panels are functioning technical experts from all branches of radio will be called upon to assist the board in its investigations. Suggestions and requests for recommendations from branches of the Government or important groups in the radio field will be considered and acted upon by the board.

Final arrangements for co-ordination of RTPB action with that of interested Government agencies, the Board of War Communications, and the Interdepartmental Radio Advisory Committee was left to an informal conference of Government officials and RTPB members and panel chairmen called by J. L. Fly, chairman of the Federal Communications Commission, at Washington, D. C., on November 17. Only questions of procedure between Government agencies and RTPB and not questions of policy for frequency allocation were scheduled for the meeting.

Mentioning various allocation problems which are apparent already, though it is too early to discuss allocation policies, FCC Chairman Fly stressed the pressure of military needs for allocation and also those of the many new radio services in aviation, communications, electronics, frequency modulation, and television. He predicted that radio would go far beyond the very high bands in use before the war and that the spectrum may be extended to entirely new territory, at least to 300,000 kilocycles and later perhaps to 30,000,000

Future Meetings of Other Societies

American Institute of Mining and Metallurgical Engineers. Annual meeting, February 20–24, 1944, New York, N. Y.

American Society of Civil Engineers. January 19–21, 1944, New York, N. Y.

American Society of Heating and Ventilating Engineers. 50th annual meeting, January 31–February 2, 1944, New York, N. Y.

American Society of Mechanical Engineers. Spring meeting, April 3–5, 1944, Birmingham, Ala. Semi-annual meeting, June 19–20, 1944, Pittsburgh, Pa.

American Society of Refrigerating Engineers. 39th annual meeting, December 7–9, 1943, Philadelphia, Pa.

Engineering Institute of Canada. Annual meeting, February 1–11, 1944, Quebec, Que.

Exposition of Chemical Industries. December 6, 1943, New York, N. Y.

National Electrical Manufacturers' Association. April 23–7, 1944, Chicago, Ill.

National Fire Protection Association. May 8–11, 1944, Philadelphia, Pa.

kilocycles. Transfer of television to the higher part of the spectrum beyond its prewar position in the 56-megacycle region was another present possibility touched upon Chairman Fly. Moreover he regarded television relays as the medium for national networks and in the future for international television. In his opinion, where wire lines are feasible, it would be economical to use wire facilities and save radio space. The problem of obtaining international standards governing frequencies and manufacture of apparatus which conform to high American engineering levels was also indicated by E. K. Jett, chief engineer of FCC.

Final decisions on allocations for civilian needs, Chairman Fly noted, would be made by the FCC while Government radio needs would be planned by IRAC which includes representatives of the Army, Navy, Coast Guard, Department of Agriculture, Department of Commerce, and other government agencies. IRAC chairman is FCC Commissioner T. A. M. Craven.

WPB and WMC Co-ordinate Production Needs and Man-Power Resources

A basic "urgency" plan to integrate production needs with man-power resources wherever and whenever conditions of urgency may develop any place in the United States have been announced by the War Production Board and the War Manpower Commission.

The plan, based on the pattern established by the Committee of War Mobilization in September, provides for establishment of two committees in any selected "urgency" labor-shortage area:

1. An area production urgency committee, primarily under the control of the WPB, determines which production in the area is of most importance. This will usually be done on a plant-by-plant basis.

2. An area man-power priorities committee, primarily under the control of the WMC, advises the United States Employment Service to allot man power from the scarce supply to those plants where it will have the greatest impact on the war effort.

PRODUCTION URGENCY COMMITTEES

Wade T. Childress has been appointed deputy vice-chairman to handle area production urgency operations.

In each critical area a committee will be established under the chairmanship of a WPB representative and will include representatives of the WMC, War and Navy Departments, Maritime Commission, War Food Administration, Army Air Forces, and such other agencies as may be invited to participate when their particular problems are involved.

Where the interests of the Smaller War Plants Corporation are concerned, the chairman of each production urgency committee will seek the views of that agency and invite its representative to attend committee meetings dealing with that corporation's problems.

No new contracts or renewals of contracts which will aggravate the existing situation will be placed in a critical area except in those cases where for technical or strategical reasons the production executive committee decides otherwise. Where this occurs, the procurement agency involved will be expected to withdraw an equivalent amount of existing work so that the total man-power demand is not increased.

MAN-POWER PRIORITIES COMMITTEES

Area man-power priorities committees shall consist of a representative each of the WMC, WPB, Selective Service System, and Navy and War Departments. If, however, other Government agencies represent important claimants for man power in the area, such as the Office of Defense Transportation, Civil Service Commission, War Food Administration, National Housing Agency, and the Maritime Commission, representatives of such agencies may be added to the committee.

The area man-power priorities committees and the area production urgency committees in critical labor areas will work in close co-operation in order that the labor available may be utilized to best advantage from the point of view of those whose job it is to see that war materials are produced in the shortest possible time with the minimum of confusion.

Production urgency committees and man-power committees have already been set up in San Diego, Los Angeles, and San Francisco, Calif.; Portland, Oreg.; Seattle, Wash.; Akron, Ohio; Detroit, Mich.; and Hartford, Conn.; and man-power committees are also functioning in Buffalo, N. Y., and Louisville, Ky.

The plan will be extended from time to time to other areas. An important phase of the plan is that the area involved will be given almost entire responsibility for solving its problem.

WAR PROGRAM . .

Navy Bulletin Lists Specialists Needed

The current bulletin, covering the special procurement programs in effect for the Navy, lists many opportunities for specialists with a record of successful training and experience in various fields. Although applicants for a commission or warrant must pass a rigid physical examination, defects which are not organic in nature and which are not likely to interfere with the performance of the specified duties entailed by the programs listed, may be waived by the Navy Department. While the age limits are not inflexible, those outside the age ranges must have exceptional qualifications. Except where otherwise indicated a college degree or years of successful experience is required. The following are among the programs listed:

S.P. 110-43 Production Expeditors. Age 30 to 50. To expedite production in plants manufacturing products under navy contracts, with a view to breaking bottlenecks. Candidates should be electrical or mechanical engineers who have specialized in one of the following fields: industrial engineering, marine engineering, ordnance, or steampower. Eight years' practical experience in any of the above fields will be accepted as a substitute for formal education. It is desirable that candidates have a thorough knowledge of production organization, methods, layout, and machines, and be familiar with production problems peculiar to the manufacture of Diesel engines, or radio and electrical equipment, or machinery, or castings and forgings.

S.P. 111-43 Production Analysts. Age 19 to 50. To analyze production methods and check progress of products manufactured under navy contract with a view to keeping navy products up to scheduled delivery. Candidates should meet one of the following sets of qualifications: electrical or mechanical engineers with four years' experience in industrial sales work or two years' actual production experience; graduates of a recognized school of business administration, experienced in business or industrial organization and management with four years' sales work or two years' production experience.

S.P. 114-43 Inspectors of Machinery. Age 30 to 45. To be assigned in the offices of supervisors of shipbuilding and inspectors of machinery for work necessitated by the control materials plan, progress work, and general expediting and administrative work. Should have had experience in a responsible position in the purchasing and procurement of materials in the purchasing departments of contractors and manufacturers covering the following fields: building trades, durable goods industries, steel products, automotive manufacturing, refrigerators, and air conditioning. Should have had executive experience. Salesmen as such are not desired for this program.

S.P. 134-43 Ship Repair. Age 21 to 42. To be assigned to duty in ship repair units for service at advance bases and afloat on repair ships. Candidates who are graduate engineers should have practical experience in at least one of the following categories: Maintenance, repair, or installation of machinery and equipment; maintenance, repair or installation of chemical equipment or experience of a nonresearch character in chemical manufacturing enterprises; Diesel engineering; steel construction, and steam engineering; the field of mechanical engineering (not design engineering); the manufacturing, maintenance, repair or installation of radio, radar, or television equipment; construction, operation, maintenance, or repair of power plants, large motors and generators, industrial electrical equipment.

S.P. 139-43 Naval Academy Instructors. Age 30 to 50 For duty as instructors at the U. S. Naval Academy to teach the following subjects: College algebra, trigonometry (plane and spherical), analytic geometry, calculus, and mechanics; chemistry and physics; radio and electrical engineering; English composition, literature, naval history, European history, government, and American diplomacy, French, Spanish, German, Portuguese, Italian, Russian, and Japanese. Candidates must have at least one degree from an accredited college or university. Must have had at least one year of successful experience in teaching one of the above subjects at a college or university of recognized standing and candidates must be eligible to continue or resume teaching at the same school. Must have sufficient personality, officer-like bearing and appearance to command the respect of the midshipmen.

S.P. 142-43 Antisubmarine. Age 21 to 35. To be assigned duties in connection with the operation and maintenance of underwater ordnance and antisubmarine devices. Candidates must have at least a bachelor's degree in electrical or radio engineering or a degree in physics with a minor in electronics or

radio. It is desirable, but not mandatory that candidates have had experience in radio repair, radio maintenance, radio operation, or have a working knowledge of electrical circuits.

S.P. 143-43 Safety Engineering. Age 30 to 50. To be assigned to duty in all phases of accident prevention in naval shore establishments. Candidates must hold an engineering degree from an accredited college and have had at least five years of practical experience in a responsible position as an industrial safety engineer. In the absence of a degree, candidates must have at least two years of satisfactory college work and ten years of practical experience in industrial safety engineering and have attained a recognized standing in that profession.

A list of Offices of Naval Procurement may be found in the June issue of *Electrical Engineering* page 278.

Mobile Power Plants Built for Navy

This is one of two mobile railway-car steam-electric power plants with a capacity of 20,000 kw which have been constructed under the direction of the Navy's Bureau of Yards and Docks by the General Electric Company, Schenectady, N. Y. Intended to supply power quickly as it is required by naval shore stations, the units can be hauled at speeds up to 40 miles per hour and their power "put on the line" within 24 hours after they are shunted into a siding. A supply of Bunker C fuel oil sufficient for two hours operation is carried, so that power can be generated even before tank cars are hauled up and connected. The apparatus used in the mobile plants is of the same type proved in service in regular central station and industrial installations throughout the United States. Generation is at 13,800 volts, and transformers are included in the units to provide voltages corresponding to those of the various electric distributing systems which the units will supply

Use of High-Quality Mica Limited by New WPB Ruling

In view of sharply declining stockpiles of the better qualities of mica, the mica-graphite division of the War Production Board, after consultation with the radio and radar division, acted recently to limit the quantities of this critical material available for production. Beginning in December, the board will undertake to provide manufacturers with amounts of high-grade mica just sufficient to maintain consumption at the average rate maintained during the first nine months of 1943.

Average consumption of good stained mica and better qualities for the first eight months of this year was more than 50,000 pounds in excess of receipts. As a result, government stocks of certain types of mica used in capacitors are at a vanishing point. Industry stocks are reduced, also, in practically all instances, to a minimum working inventory.

The supply of six types of mica, used almost entirely for capacitor films, is shortest. They are: number 4 clear and slightly stained block mica; numbers 5 and $5^1/_2$ fair stained block mica; number $5^1/_2$ good stained block mica; and numbers 5 and $5^1/_2$ fair stained film mica. The insufficiency of these types of mica has compelled the WPB to draw on other types of mica as substitutes. Although the mica-graphite division is not aware of any instance yet in which necessary war production has been delayed because mica could not be provided, it will not be long before suppliers will be unable to meet the demands for mica.

The new policy is not expected to limit mica capacitor production, since the restriction applies only to block mica of good stained quality or better and to film mica of second quality or better. Capacitor manufacturers will have the choice of restricting their production to the number of capacitors that can be made from their allocations of the usually accepted qualities of mica, or of using lower qualities of mica to expand production. The results of the capacitor research project conducted by the National Research Council at Bell Telephone Laboratories, Inc., (*EE, Nov. '43, pp. 511-12*) will be made available to capacitor manufacturers as soon as results have been obtained for each type of mica, to aid them in determining which type of the various lower qualities can be used to best advantage.

The system will be subject to revision quarterly, at which time the ratio of new allocation for capacitor production may be increased or decreased, depending on receipts of mica in the government stockpile during the preceding quarter. Stocks of lower qualities have been accumulating rapidly during the year. On stained quality, the next lower grade to good stained quality, stocks have increased from 370,000 pounds on January 1 to 1,160,000 pounds on August 1, 1943.

Army and Navy Incentive Films Available to War Plants

War plants wishing to stress to their employees the importance of the worker and his job and his close relationship with the fighting men may secure through the War and Navy Departments industrial incentive films designed especially for war workers and their families.

Included in the subjects are action pictures of the Navy's newest and deadliest antisubmarine weapon, the destroyer escort; landing of the Marines on Guadalcanal; "The Life and Death of the Hornet"; captured German films; landing supplies and evacuating the wounded by Air Transport Command in New Guinea; bombers over North Africa; and the battle of Britain.

These films, which are "restricted" and cannot be viewed in commercial theaters, are made available through a national distribution system of film exchanges in 300 key cities throughout the country. The films are available in both 16-millimeter and 35-millimeter sound-track prints. A nominal fee of one dollar for three reels or less in any one shipment is charged to cover costs of transportation, handling, insurance, and maintenance.

Information on the Navy films may be secured by writing to the chief of the industrial incentive division, Navy Department, 2118 Massachusetts Avenue, Washington, D. C., and information on Army films may be obtained from the chief of the industrial services division, War Department, Bureau of Public Relations, Room 2-*B*-852, the Pentagon, Washington, D. C.

Production of Electric Instruments Increases 4,000 Per Cent

Production of electric indicating and measuring instruments essential to the maintenance of a mechanized war has increased 4,000 per cent since 1940—from 700,000 to 28,000,000 units an-

nually, H. P. Sparks, chairman of the electric-measuring-instrument section, reported at a recent convention of the National Electric Manufacturers Association. Nor will production stop at that level. War Production Board estimates call for an additional 40 per cent increase in 1944 and this scheduled output will bring the level up to 39,000,000 units annually.

Modern warfare requires millions of accurate instruments in order to keep the individual units of the armed forces functioning properly, he explained. A single large bomber may carry as many as 250; one battleship may use 1,000; a submarine mounts 150; and even a tank requires 10. The prominence of the air in modern warfare has brought about a corresponding increase in the need for instruments to guide and control antiaircraft weapons.

To boost production of war-vital combat and industrial instruments, the WPB issued limitation orders, drawn up in co-operation with an NEMA committee, which shut down production of certain nonessential meters and channeled all new instrument orders through a central control for allocation to industry. Standardization and simplification also played major roles in enabling instrument builders to meet their goals. A special war committee on electric indicating instruments was set up by NEMA and the American Standards Association, under the chairmanship of R. B. Shepperd of WPB's simplification bureau. This committee drew up a simplification standard which eliminated as many as 85 per cent of the previously listed variations of instruments.

New Generator Operates at Grand Coulee

Keeping abreast of the steadily mounting power demands of the war industries of the Pacific Northwest, the Bureau of Reclamation has put into commercial production at Grand Coulee Dam another generator rated at more than 100,000 kw, Harold L. Ickes, Secretary of the Interior, announced recently.

According to Secretary Ickes, "the power installed at Grand Coulee during the two years since the first large generator was put into service and the installations at Bonneville Dam farther down the Columbia River were made has met a definite war need and has been responsible in a large measure for the establishment of major war factories in the Pacific Northwest."

In this time the Bureau has made available for war production in the West more than 1,000,000 kw of new hydroelectric power with many of the new generators in operation two to five years ahead of schedule.

The latest addition at Grand Coulee brings the installed capacity of the plant to over 700,000 kw, making it third largest in the United States and fourth largest in the world. The present capacity at the dam is surpassed in the United States only by Boulder Dam power plant and by a steam plant serving the New York metropolitan area. Moreover, with the new installation the rated capacity of the Bureau of Reclamation's 30 power plants in 11 western States passes 2,000,000 kw—equivalent to the total capacity of all public utilities in the 11 far western States in 1920 and nearly as much as that of all plants in the United States when federal reclamation was established in 1902.

WPB Publishes Salvage Manual

The first comprehensive practical manual on industrial salvage ever prepared, "Salvage Manual for Industry," recently published by the technical service section, industrial salvage branch, salvage division of the War Production Board, is now being distributed to industry. There are chapters on organizing and planning the salvage department, administrative factors, methods of handling metal scrap, nonmetallic waste, case histories demonstrating exemplary practice, and practical hints for handling specific waste materials.

The well-illustrated volume was prepared and edited by an editorial board of practical industrial salvage engineers and business paper editors comprising the following:

Editors—Robinson D. Bullard of The Bullard Company, and Fred P. Peters of *Metals and Alloys*

Associate editors—H. E. Blank, Jr., of *Modern Industry*, Arthur M. Perrin of National Conveyors Company, E. J. Tangerman of McGraw-Hill Publishing Company, and R. A. Wheeler of The International Nickel Company, Inc.

Managing editor—John O. Emerson of the industrial salvage branch, WPB

Assisting the editors either with direct contributions or advice was a corps of some 40 engineering or salvage experts. No effort has been spared to cover every possible phase of practical industrial salvage operations and to present the most reliable and authoritative information about them. The book may be secured from the Superintendent of Documents, Government Printing Office, Washington, D. C., at 50 cents per copy.

High-Speed Camera Uncovers Split-Second Mechanical Action

A speed of 8,000 frames or exposures per second is attained with the Fastax camera developed since the war by Bell Telephone Laboratories, Inc., as a research tool. Imperfections in equipment hitherto undetectable are exposed by the great speed of the new camera. Either 8-millimeter or 16-millimeter film may be employed in the camera and it is adaptable to both black and white and color photography and to the photography of self-luminous objects. The intensity of light required for the extra-high-speed pictures is obtained by stepping up the voltage for high-intensity lamp filaments to within a few degrees of their melting temperatures. Although its exposure rate is twice as fast, the Fastax camera resembles in other ways the high-speed camera described in *Electrical Engineering*, November 1940, pages 448–50. Its film speed of from 3 to nearly 70 miles an hour and the continuous film-drive mechanism are the same. Exposure of successive frames in both cameras is accomplished by a revolving prism, and images and film are synchronized as they pass the film gate during the exposure period.

Most of the work being done with the new camera involves secret war projects. It has also been used to photograph action of the vocal chords and to show the explosive short-circuiting of wires carrying heavy currents. It has revealed a cause of imperfection in telephone equipment by showing that the movable part of signal-relay devices was rebounding after the initial contact, and that this was causing extra or false signals in the equipment.

United Nations' Service Reviews Postwar Planning. Part X of Research and Postwar has been issued by the United Nations Information Service, 610 Park Avenue, New York 20, N. Y. The survey is published in two sections—one listing the agencies in the United States and Great Britain which are devoted to postwar planning and the other containing an up-to-date bibliography on the subject. The location, officers, sponsors, background and activities, and publications, if any, are given for each organization. The bibliography is divided into three periods: war, immediate postwar, and reconstruction. The literature pertaining to the periods is arranged under headings denoting the problems peculiar to each. Each section of the survey is priced at 75 cents.

JOINT ACTIVITIES

American Standard for Fixed Composition Resistors Approved

A new American War Standard *C* 75.7, Fixed Composition Resistors, for component parts for military radio and electronic equipment, being developed at the request of the War Production Board, with the co-operation of the War and Navy Departments and the radio industry, has been approved for use by the United States Signal Corps and the United States Navy, Bureau of Ships, radio division, for use in procuring resistors for radio and electronic equipment.

The specification covers fixed composition resistors suitable for use in all nonspecialized applications, in communications and electronic equipment. Performance requirements, test methods, standard dimensions, standard resistance values, and ratings for these resistors of the quality required by the Armed Forces are

contained in this standard. It is the first time an agreement has been reached between the resistor manufacturer and the resistor user as to just what the performance of general-purpose "garden-variety" fixed composition resistors should be. It is expected that this Standard will be used as a guide in the preparation of new manufacturing facilities which are now being expanded because of a service shortage of resistor parts.

The committee which prepared the Standard was headed by F. K. Priebe, Signal Corps Laboratory, Fort Monmouth, N. J., and included representatives from industry and the United States Army and Navy. The Standard may be obtained without charge for procurement purposes only from the government agency concerned. It may be obtained for 60 cents from ASA, 29 West 39th Street, New York 18, N. Y.

ASA Observes Silver Anniversary

American Standards Association will observe the 25th anniversary of its founding at its annual luncheon meeting December 10, in the Hotel Roosevelt, New York, N. Y. The association, started as a result of the production problems of the last war, has completed more than 40 emergency jobs for the armed services and industry in the past year and is engaged in many others.

Clifton E. Mack, director of procurement, United States Treasury, will discuss the use of standards for bringing government requirements more nearly in line as a part of the American industrial system at the meeting December 10. Mr. Mack is in charge of all government lend-lease purchasing. R. E. Zimmerman, president of ASA, will talk on postwar changes and developments, and H. S. Osborne (F '21) chairman of the Standards Council, will report on the year's work. In commemoration of its silver anniversary, ASA is inviting all who wish to attend the annual meeting, whether they are members of the organization or not. Information about the meeting may be obtained from ASA, 29 West 39th Street, New York 18, N. Y.

EDUCATION • • •

Wayne University Acquires Hooker Scientific Library

The Kresge Foundation has granted to Wayne University the sum of $100,000 for the establishment there of a scientific library, announces David D. Henry, executive vice-president of the university. The grant, together with an equal amount contributed by interested organizations and individuals, will be used to purchase and modernize the Hooker Scientific Library, now located at Central College, Fayette, Mo. The modernized collection will be known as the Kresge-Hooker Scientific Library.

According to Doctor Whitehouse, "The Hooker Library, built upon the basic collection of the noted scientist, Samuel C. Hooker, is not only recognized as one of the world's most complete collections of books and journals on chemistry, but is also of great value to the related sciences and to engineering. It is widely used by research scientists in all parts of the United States who utilize the reference and translation services supported by the Friends of the Hooker Scientific Library."

The Kresge Foundation funds will be handled through the Wayne University Foundation, according to Gordon W. Kingsbury, secretary of that organization.

OTHER SOCIETIES •

NEMA Issues New Standard. The new Feeder-Voltage-Regulator Standards, publication 43–86, recently issued by the National Electrical Manufacturers Association, comprises all the standards of national character pertinent to feeder and step-type voltage regulators in a single 28-page pamphlet. Among the subjects treated are: The effect of altitude on temperature rise, rating, grounding, insulating materials, tests, performance specifications, bushing characteristics, guides for loading, efficiencies and losses, and terminal markings. The pamphlet is completed with a section devoted to the definition of terms. Copies may be obtained from NEMA headquarters, 155 East 44th St., New York, N. Y., at 75 cents a copy.

U. S. Committee of ICI Re-elects Officers

At the annual meeting of the United States national committee of the International Commission on Illumination November 10, 1943, the following officers were re-elected:

President—Preston S. Millar (M '13) of Electrical Testing Laboratories, Inc.

Vice-president—F. C. Breckenridge of National Bureau of Standards.

Secretary-treasurer—G. H. Stickney (F '24) consulting engineer.

Executive Secretary—A. A. Brainerd of Philadelphia Electric Company.

This United States committee has published an all-English version of the proceedings of the international meeting held in Holland in 1939. Copies, recently made available, may be obtained through the executive secretary. These record the latest stage of development of lighting throughout the world up to the beginning of the war and have reference value for people in the lighting industry.

The United States committee is engaged in cultivating, in behalf of the international body, arrangements for technical interchange of lighting information with leading engineers in the other Americas.

Industrial Safety Engineering Division of NSC Reorganized

The National Safety Council has reorganized the activities of its industrial safety engineering division to provide more practical and specific assistance for accident and health problems in modern industry, according to Walter S. Paine, vice-president for industrial safety.

The new plan makes use of the industrial membership sections as centers for the accumulation and distribution of all safety information in their respective fields. Such information, in the form of safe practices pamphlets, industrial data sheets, engineering summaries, safety instruction cards, posters, and other technical materials must be kept up to date and must be designed to fit the exact needs of those who will use it.

Each safety engineer on the staff has been appointed as staff contact for one or more of the industrial sections, in accordance with his experience and knowledge.

Division engineers and the sections to which they have been assigned are:

J. M. Roche, director of the industrial division; J. C. Stennett, assistant director of the industrial division, secretary of the American Society of Safety Engineers, and contact man for the aircraft manufacturing industries and air-transport companies; C. D. Bridges, paper, pulp, and petroleum industries; F. E. Frazier, construction, mining, cement, and quarry industries; A. M. Baltzar, food, meat-packing, and textile industries; E. M. Jasper, metals industries and public utilities; B. A. Grainger, automotive, machine-shop, and marine industries, and power-press problems; and F. Van Atta, chemical, rubber, and refrigerator industries.

In the future, additional engineers will be added to the staff—one to have full-time assignment to problems in the mining industry, another to handle railroad safety problems, and a third who will act on assignments relating to industry generally.

IRE Elects 1944 Officers

Election of Hubert M. Turner (M '20) associate professor of electrical engineering, Yale University, New Haven, Conn., as president of the Institute of Radio Engineers, was announced recently by the board of directors of that organization.

Also elected were R. A. Hackbusch, vice-president in charge of radio, Research Enterprises, Ltd., Leaside, Ont., as vice-president of the institute; and for three-year terms as directors: R. F. Guy, engineer, National Broadcasting Company, New York, N. Y., L. C. F. Horle (F '35) consulting engineer, New York, N. Y., and W. C. White (M '30) director of the electronic laboratory, General Electric Company, Schenectady, N. Y.

AIME Elects Officers for 1944

Election of C. A. Fulton, president of the Southern Phosphate Corporation, Baltimore, Md., as 61st president of the American Institute of Mining and Metallurgical Engineers was announced recently by A. B. Parsons, executive secretary of AIME. Other officers elected were: Vice-presidents, J. L. Christie, metallurgist and manager, Handy and Harmon, Bridgeport, Conn. and J. R. Van Pelt, Jr., geologist and technical director, Museum of Science and Industry, Chicago, Ill.; and six directors, M. H. Fies, mining engineer and vice-president in charge of mining division, De Bardeleben Coal Corporation, Birmingham, Ala.; H. T. Hamilton, assistant to the president, New York Trust Company, New York, N. Y.; J. C. Kinnear, mining and metallurgical engineer, general manager, Nevada Mines Division, Kennecott Copper Corporation, McGill, Nev.; W. E. Pratt, geologist, vice-president, Standard Oil Company of New Jersey, New York, N. Y.; J. R. Suman, vice-president in charge of production, Humble Oil and Refining Company, Houston, Tex.; and R. W. Thomas, general manager, Nevada ConsolidatedCopperCorporation,Ray,Ariz.

LETTERS TO THE EDITOR

INSTITUTE members and subscribers are invited to contribute to these columns expressions of opinion dealing with published articles, technical papers, or other subjects of general professional interest. While endeavoring to publish as many letters as possible, Electrical Engineering reserves the right to publish them in whole or in part or to reject them entirely. Statements in letters are expressly understood to be made by the writers. Publication here in no wise constitutes endorsement or recognition by the AIEE. All letters submitted for publication should be typewritten, double-spaced, not carbon copies. Any illustrations should be submitted in duplicate, one copy an inked drawing without lettering, the other lettered. Captions should be supplied for all illustrations.

Planning for Things to Come

To the Editor:

"It is hardly lack of due proce-s for the government to regulate that which it subsidizes."

United States Supereme Court re Wickard vs. Filburn

I have just finished reading the Institute's past-president's most interesting and thought-provoking discourse on "Planning for Things to Come" (*Electrical Engineering*, August 1943, pages 333–337), and I would like to add a mite to the discussion.

Yes, there must be planning for things to come as there always has been, even though no one seems to know what things will come. It seems to me, however, that Doctor Osborne neglected to mention the most limiting and controlling factor in all such planning. That factor is the future relation of the Federal Government to private or free enterprise. The crucial question that must be answered before any effective plans can be formulated is, will business and industry be permitted to plan freely and to execute the plans formulated, or will government agencies control. If the latter is the case, then the kind of planning suggested by Doctor Osborne will have little influence on "things to come." For example, the engineers of the American Telephone and Telegraph Company will undoubtedly make plans for improving, increasing, and enhancing the value of the services of the company's communication systems, but it cannot co-operate with the Western Union Telegraph Company without running afoul of the national antitrust and monopoly laws. This, however, is only an example.

Furthermore, if the government subsidizes industry and business, either directly or indirectly, it will control them and direct their activities in accordance with the declaration of the Supreme Court cited above, and the planning for "things to come" will be done by governmental agencies, and it will be a waste of effort to make other plans. Likewise, if the government continues to compete with business and industry which provide the taxes to pay for such competition, private enterprise will gradually decline and finally fade away and die. The evidence in support of these propositions is all around us.

If, therefore, the engineers hope to have controlling influence on "things to come," their first efforts should be devoted to the making of plans for freeing enterprise from the socialistic controls of the government. Obviously the AIEE board of directors have already recognized the necessity for such efforts by passing resolutions against the adoption of the Kilgore and Patman bills. That is a step in the right direction, but resolutions alone are not enough. Resolutions must be followed up by action, not by the board of directors alone, but by the individual members of the Institute and other like-minded persons.

Doctor Osborne further suggests the affiliation of all engineering societies and organizations into some form of a super organization whose duties will be to place "emphasis on ministry to the people rather than on direct services to its members" and "to give advice to the numerous agencies of government on the engineering phases of public questions." That was the objective of the now defunct American Engineering Council. Its failure was not due to defects in technical organization but to its inactivity. It acted on the assumption that its ministry to both the engineers and society would be most effective if it waited to be asked by government agencies for advice "on the engineering phases of public questions," but such questions seldom came, and when they did come it was too late for the advice to influence governmental policies.

Any such organization, to be effective in its ministry, must be active and not passive. Engineers are also members of the body politic and if they expect to influence governmental action in other fields than mere planning of engineering structures, they must use not only engineering technique, but the technique that will be most effective. The use of this effective technique the engineering societies have not only deprecated, but eschewed for a long time.

C. M. JANSKY (F'32)

(Professor emeritus of electrical engineering, University of Wisconsin, Madison)

NEW BOOKS • • •

The following new books are among those recently received from the publishers. Books designated ESL are available at the Engineering Societies Library; these and thousands of other technical books may be borrowed from the library by mail by AIEE members. The Institute assumes no responsibility for statements made in the following summaries, information for which is taken from the prefaces of the books. All inquiries relating to the purchase of any book reviewed in these columns should be addressed to the publisher of the book in question.

Aircraft Year Book for 1943. 25th annual edition. Howard Mingos, editor. Aeronautical Chamber of Commerce of America, Shoreham Building, Washington, D. C.; distributors, Lanciar Publishers, New York, N. Y., 1943. 728 pages, illustrations, etc., 9 by $5^1/_2$ inches, cloth, $5. (ESL.)

As in previous editions this 1943 publication presents a record of events and developments in the aviation field during the past year. It covers the war in the air, the United States Army and Navy air forces, the Civil Air Patrol, air-force training, governmental activities, and the air lines in war transport. The latter part of the book is devoted to aircraft designs, directories of manufacturers in the field, and statistical information.

Manual of ASTM Standards on Refractory Materials. Prepared by ASTM Committee C-8 on Refractories. American Society for Testing Materials, 260 S. Broad Street, Philadelphia, Pa., 1943. 201 pages, illustrations, etc., 9 by 6 inches, paper, $1.50; cloth, $1.75. (ESL.)

Designed to give all of the ASTM standards on refractory materials—specifications, methods of physical tests, chemical analysis, and definitions—this extensively revised and enlarged publication also includes pertinent data developed by the committee and other supplementary information of service to those concerned with refractories.

Airport Construction and Operation Reference. 1943–44 annual edition. Occidental Publishing Company, Los Angeles and San Francisco, Calif., and New York, N. Y., 1943. 96 pages, illustrations, etc., 12 by 9 inches, cardboard, $2. (ESL.)

In the text of this book the subjects of airport design, seaplane bases, war airfields, airport surfaces, buildings, lighting, communications, and traffic control are presented by useful summaries of practice. In addition there is a buyer's directory of airport-equipment manufacturers and a directory of associations and agencies.

Structures of Metals. By C. S. Barrett. McGraw-Hill Book Company, New York, N. Y., and London, England, 1943. 567 pages, illustrations, etc., 9 by 6 inches, cloth, \$6. (ESL.)

Crystallographic methods for investigating the structure of metals are discussed. The first four chapters explain the fundamentals of crystal lattices and projections and the general principles of the diffraction of X rays from crystals. Chapters 5 to 7 cover the technique of X ray diffraction. The latter half of the book is devoted to the results of research along specific lines of current interest, including a chapter on electron diffraction. The book is intended for graduate courses.

Maximum Utilization of Employed Manpower. (Research Report Series No. 68.) Princeton University, Industrial Relations Section, Princeton, N. J., 1943. 46 pages, $9^1/_2$ by 6 inches, paper, \$1. (ESL.)

This publication constitutes an outline listing a wide range of symptoms or ailments which are likely to accompany or cause underutilization of employed labor. Most of the subheadings, however, indicate positive steps, drawn from widespread company experience, which have proved successful remedies. A detailed bibliography is appended.

Engineering Drawing Problems. By I. N. Carter and H. L. Thompson. International Textbook Company, Scranton, Pa., 1943. 142 pages, plates, etc., $8^1/_2$ by 12 inches, stiff paper, \$2.25. (ESL.)

A carefully selected group of drafting exercises designed to be used with the text, *Engineering Drawing—Practice and Theory*, is presented by the same authors. In addition to the problem plates already made up, there are several blank plates for special work, and a number of sheets of tracing paper are provided for tracing practice.

Circuit Analysis of A-C Power Systems. Volume I. Symmetrical and Related Components. By E. Clarke. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 540 pages, diagrams, etc., 9 by $5^1/_2$ inches, cloth, \$6. (ESL.)

In the two-volume set of which this is the first volume, the methods of solving unbalanced power-system problems by means of components are analyzed and discussed in detail. Volume I deals largely with the determination of currents and voltages of fundamental frequency in power systems, by means of symmetrical and related components, including overhead transmission circuits, transformers, and synchronous machines. The use of equivalent circuits and the solution of practical problems are emphasized.

Electricity and Its Application to Civilian and Military Life. By Charles A. Rinde. Harcourt, Brace and Company, New York, N. Y., and Chicago, Ill., 1943. 466 pages, illustrations, $9^1/_2$ by $6^3/_4$ inches, cloth, \$2.50.

This book is organized around the War Department's *Outline, Fundamentals of Electricity* (*PIT-101*), but the outline has not been followed slavishly. The book thus provides a broad foundation for the fields of specialization suggested by the various technical and field manuals. Since all the principles underlie equally the civilian and military uses of electricity, both civilian and military applications have been stressed throughout.

Treatment of Experimental Data. By A. G. Worthing and J. Geffner. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 342 pages, illustrations, etc., $9^1/_2$ by 6 inches, cloth, \$4.50. (ESL.)

As an aid to scientists and engineers in presenting experimental data clearly and usefully, this book presents and discusses the following topics: rules for graphing; methods of smoothing and tabulating; a moderately extended treatment of precision indexes; the essentials of correlation; Fourier series and harmonic analysis as a means of representing data; and the use of determinants as a means of simplifying computations.

Structural Frameworks. By C. T. Morris and S. T. Carpenter. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 272 pages, illustrations, etc., 9 by $5^1/_2$ inches, cloth, \$4. (ESL.)

This book, which is intended for advanced students, is concerned with the analysis of some complex problems that arise in the design of buildings and structural frameworks, including industrial buildings and radio and transmission towers. Numerous examples are worked out to illustrate the methods used.

Slide Rule Simplified. By C. O. Harris. American Technical Society, Chicago, Ill., 1943. 250 pages, etc., $8^1/_2$ by $5^1/_2$ inches, cloth, \$2.50; with slide rule, \$3.50 (ESL.)

The practical manipulation of the slide rule is explained in detail. The first eight chapters cover the relatively simple straight arithmetical operations for the beginner. Succeeding chapters deal with the handling of trigonometrical relations and other more complex operations. The logarithmic basis of the functioning of the slide rule is explained for those who are interested.

Metal Forming by Flexible Tools. By C. J. Frey and S. S. Kogut. Pitman Publishing Corporation, New York, N. Y., and Chicago, Ill., 1943. 193 pages, illustrations, etc., $9^1/_2$ by 6 inches, cloth, \$3. (ESL.)

The characteristics of the flexible tool, developed and mainly applied in the aircraft industry, to meet the frequent changes in design necessitated by war, are low first cost and rapidity of manufacture, and the ability to adhere to sheet-metal tolerances so as to permit interchangeability. The answer to the need for flexible tooling has been found in the rubber press, the drop hammer, the power brake, the stretch press, and the Anderson method of forming by drawing, described in detail in this book.

Marconi, Pioneer of Radio. By D. Coe. Julian Messner, New York, N. Y., 272 pages, illustrations, etc., 9 by 6 inches, cloth, \$2.50. (ESL.)

Marconi's great influence on the development of wireless transmission is told in narrative style. Much biographical detail is included with the character of the man himself emphasized. Important and dramatic incidents connected with Marconi's life and the rise of radio as a useful science increase the interest of the book.

Management of Manpower. By A. S. Knowles and R. D. Thomson. The Macmillan Company, New York, N. Y., 1943. 248 pages, illustrations, etc., 9 by $5^1/_2$ inches, cloth, \$2.25. (ESL.)

The text, part of a larger volume on industrial management, is reproduced for those whose primary attention is devoted to handling workers. It discusses the modern tools and techniques available for the effective and intelligent handling of man-power problems leading toward greater efficiency, higher production, and better co-operation of the workers. Job evaluation and merit rating are emphasized.

General Physics. By O. Blackwood. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 622 pages, illustrations, etc., $8^1/_2$ by $5^1/_2$ inches, cloth, \$3.75. (ESL.)

The whole field of college physics is covered in this elementary text. The major divisions are: mechanics; molecular physics and heat; vibrations, wave motion, and sound; light; electricity and magnetism; and the new physics. Emphasis is placed on the practical illustration of physical principles by examples from everyday life.

Metallography of Aluminum Alloys. By L. F. Mondolfo. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 351 pages, illustrated, 9 by $5^1/_2$ inches, cloth, \$4.50.

Four main sections of this book cover, respectively: the equilibrium diagram of aluminum alloys; the technique of macro- and microexamination; the normal structure of the commercial alloys of aluminum; the effect of fabricating on the microstructure, with references to macrostructure and actual practices. Since the book is intended for the plant metallurgist rather than the student, no details are given on general metallurgy and metallography. There is a large classified bibliography.

Electronic Interpretations of Organic Chemistry. By A. E. Remick. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 474 pages, illustrated, 9 by $5^1/_2$ inches, cloth, $4.50. (ESL.)

The main purpose of this book is to show how electronic theories of organic chemistry may be combined with such modern developments in physical chemistry as the quantum-mechanical concept of resonance and the transition-state theory of reaction rates. The work is intended as a review and an advanced textbook in which those developments in the field of physical and theoretical chemistry that seem to offer new and useful methods of attacking the problems of preparative organic chemistry are presented.

Electrical Engineering. By E. M. Strong. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 391 pages, diagrams, etc., 9 by $5^1/_2$ inches, cloth, $4. (ESL.)

An introductory presentation of basic concepts essential to the clear understanding of electrical-engineering problems. It includes an introduction to alternating current and voltage as part of this basic material. A knowledge of the calculus involved in the explanation is required of the student. Detachable work sheets containing useful graphs are provided at the end of the book.

Blueprint Reading for the Shipbuilding Trades. By A. E. Niederhoff. McGraw-Hill Book Company, New York, N. Y., and London, England, 1943. 87 pages, illustrations, etc., 11 by 8 inches, cloth, $2. (ESL.)

The fundamentals of blueprint reading necessary for the shipbuilding trades are covered in a simple and concrete manner. Orthographic projection, alphabet of lines, symbols, abbreviations, and ship terms are included. Actual prints used in shipyards mainly for construction of Liberty ships are used as examples.

Introduction to Heat Engines. By E. A. Allcut. University of Toronto Press, Toronto, Ont., Canada, 1943. Paged in sections, illustrated, $9^1/_2$ by 6 inches, cloth, $2.75. (ESL.)

This book provides a concise interesting introduction to the field of heat engines indicating the existence of the same general scientific principles in all types of heat engines. Each chapter is illustrated by applications to steam engines, turbines, air compressors, and internal-combustion engines, whose similarities as well as differences are pointed out. Chapter four is an excellent brief historical survey.

Organization for Metropolitan Planning. American Society of Planning Officials, Chicago 37, Ill., 1943. 73 pages, illustrated 10 by 7 inches, paper, $1. (ESL.)

This pamphlet contains the four prize-winning essays in a national competition for the best proposal for the organization and operation of a regional council in a metropolitan area. The essays are presented to stimulate thinking upon a problem that is becoming increasingly acute as the tax base moves out from our cities, while these are called upon to provide social services upon an increasing scale.

Aeroplane Production Yearbook and Manual I. Edited by G. W. Williamson, foreword by Sir C. Bruce-Gardner. Paul Elek Ltd., Africa House, Kingsway, London, W.C.2, England, 1943. 564 pages, illustrated, $8^1/_2$ by $5^1/_2$ inches, linen, 40s.10d., 41s.6d. abroad. (ESL.)

The purpose of this volume is to provide information regarding production methods in a compact and accessible form. The use and treatment of aircraft materials are described, general and specialized manufacturing processes are explained, and the construction and characteristics of the varied types of airplane equipment are discussed. There is a large bibliography which includes numerous abstracts.

Practical Radio Communication. By A. R. Nilson and J. L. Hornung. Second edition. McGraw-Hill Book Company, New York, N. Y., and London, England, 1943. 927 pages, illustrated, 9 by $5^1/_2$ inches, Fabrikoid, $6. (ESL.)

Basic radio principles are concentrated in the first eight chapters of this comprehensive work. The practical application of these principles to aviation radio, broadcasting, and marine radio follow in the order given. Important additions in this edition include material on amplifiers, the cathode-ray oscilloscope, antenna arrays, ultrahigh-frequency theory and practice, frequency modulation, and direction finders.

Tungsten. American Chemical Society Monograph 94. By K. C. Li and C. Y. Wang. Reinhold Publishing Corporation, New York, N. Y., 1943. 325 pages, illustrated, $9^1/_2$ by 6 inches, cloth, $7. (ESL.)

This volume, written by the leading authority on tungsten, covers its subject thoroughly. The geology of the ore deposits, ore dressing, metallurgy, and chemistry are discussed. A chapter is devoted to analysis. Further chapters consider the industrial uses of tungsten, substitutes for tungsten in steel alloys, and the economics of the tungsten industry. The chapters have useful bibliographies.

Hyper- and Ultrahigh-Frequency Engineering. By R. I. Sarbacher and W. A. Edson. John Wiley and Sons, New York, N. Y.; Chapman and Hall, London, England, 1943. 644 pages, illustrated, 9 by $5^1/_2$ inches, cloth, $5.50. (ESL.)

All phases of hyperfrequency engineering are discussed in considerable detail, including the generation, transmission, and reception of quasi-optical waves. Following the basic electromagnetic theory are chapters on wave guides, transmission-line theory, cavity resonators, horns and reflectors, vacuum-tube behavior, and applications of tubes. A large bibliography is included.

The J. & P. Switchgear Book. Volume 2. By R. T. Lythall. First edition. Johnson and Phillips Ltd., Charlton, London, England, S.E.7, 1943. 227 pages, illustrated, 9 by 6 inches, cloth, 15s. plus postage. (ESL.)

The new volume of this well-known work on switchgear is planned, like the first, to supply practical information for the needs of nonspecialists. Volume 2 supplements volume 1 by covering some items omitted in it and by giving information on later developments.

Planning and Postwar Planning—State Organizations. American Society of Planning Officials, Chicago 37, Ill. 34 pages, manifold copy, 11 by $8^1/_2$ inches, paper, $1.

This is a directory giving the names of officials and members, and office addresses of these organizations.

PAMPHLETS . . .

The following recently issued pamphlets may be of interest to readers of "Electrical Engineering." All inquiries should be addressed to the issuers.

The Financial Record of the Electric Utility Industry 1937–42. Federal Power Commission, Washington, D. C., 10 pages, no charge.

Some Comments on Emergency Electrical Products. Underwriters' Laboratories, Inc., 207 East Ohio Street, Chicago 11, Ill., 11 pages.

Pipe and Tube Bending Handbook. Copper and Brass Research Association, 420 Lexington Avenue, New York 17, N. Y., 80 pages.

Fundamentals of Electronic Control for Resistance Welding. Industrial Control Division, General Electric Company, Schenectady, N. Y., 44 pages.

The Story of the Turbine. General Electric Company, Schenectady, N. Y., 24 pages.

Wartime Fires, fourth edition. National Fire Protection Association, 60 Batterymarch Street, Boston 10, Mass., 20 pages, 10 cents.

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Overvoltage Protection of Current-Transformer Secondary Windings and Associated Circuits

R. H. KAUFMANN
MEMBER AIEE

G. CAMILLI
MEMBER AIEE

Synopsis: It has long been recognized that excessive potentials may be developed in current-transformer secondary windings under unusual conditions, such as open circuits. Recent experience discloses that dangerous overvoltages (several thousand volts) may be produced as a result of normal switching operations on circuits containing lumped capacitance.

A simple procedure for circuit analysis and evaluation of approximate voltage magnitude for the switching transient case is reported. For easy reference, there are included tables of calculated secondary voltage magnitudes covering a broad range of application.

Under certain conditions, overvoltage protection is desirable and important. Aside from the potential hazard to life, current-transformer circuit insulation may be damaged, yet not be evident immediately. Performance at normal rated current may not be noticeably impaired, yet serious failure may occur in the presence of fault-current flow, thus nullifying the action of current-actuated protective relays.

The characteristics of a new overvoltage protector expressly designed for current-transformer protection is presented. With this device current-transformer secondary voltages are limited to moderate values. The protector is small and compact, and easily applied to existing as well as new current-transformer installations The characteristics are permanent, not affected by repetitive operation, and result in negligible ratio error in the normal operating current range.

Sources of Excessive Voltage

EXCESSIVE potentials in current-transformer secondary circuits may appear as a result of:

1. Open-circuited secondary winding.
2. Switching transients in the presence of lumped capacitance in the power circuits.
3. Fault current flow.
4. Lightning impulse currents.
5. Steep front current transients of other origin.

Open-Circuited Secondary Winding

The circulation of rated current in the primary winding of a current transformer with its secondary winding open-circuited will give rise to a persistent excessive voltage, which may be high enough to be dangerous to apparatus and circuit insulation, as well as to any person who may come in contact with the circuit.

In the absence of secondary current flow, all of the current-transformer primary current is exciting current. Although only a small portion of normal rated primary current is sufficient to saturate the core, there is a short interval each half cycle as the current passes through zero that the magnetic flux is very rapidly whisked from saturation value in one direction to saturation value in the other direction. It is the exceedingly rapid rate of change of flux during this short interval which is responsible for the high open-circuit voltage. The secondary open-circuit voltage magnitude is a function of current-transformer magnetic design, is increased by higher operating frequency, is increased by reduced secondary current rating, but is limited to a maximum value of 1.41 (0.866) (E_L) (R). Potentials in excess of 2,000 volts, with rated primary current flow, may be frequently expected. Figure 2 typifies the resulting peaked voltage wave.

Switching Transients in the Presence of Lumped Capacitance in the Power Circuit

Excessive overvoltages in current-transformer secondary circuits have been observed in connection with the switching of power circuits with which are associated lumped capacitance connected either line to line or line to neutral. A description of the manner in which such voltages are generated, together with a simple procedure for evaluating the probable voltage magnitude, will be of interest.

A de-energized capacitor when switched on to an energized power circuit initially assumes the appearance of a short circuit since the capacitor terminal potential is incapable of being changed instantaneously by any finite amount. It follows that, at the instant the switch is closed, the entire system voltage must be absorbed in the circuit impedance between the capacitor and the source of supply. Current-transformer circuits constitute a part of this interconnecting impedance.

The transition between the initial short-circuit behavior and the normal steady-state normal frequency performance takes the form of a high-frequency oscillation which is rapidly damped by circuit resistance. The physical size of the lumped capacitance has relatively little influence on the transient-voltage magnitude unless small enough to become comparable with distributed capacitance in the interconnecting circuit impedance but does influence directly the frequency of the transient oscillation.

A simple procedure for circuit analysis for evaluating expected current-transformer secondary voltages is contained in Appendix A. It will be noted that the high transient frequency which invariably accompanies the production of excessive current-transformer voltages permits circuit resistance to be ignored. It will be

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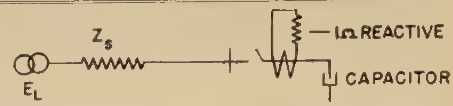
R. H. KAUFMANN is in the industrial engineering department, General Electric Company, Schenectady, N. Y., and G. CAMILLI is research and design engineer, General Electric Company, Pittsfield, Mass.

observed that the current-transformer secondary voltage is influenced prominently by power-circuit operating voltage, power-circuit inductive reactance, current-transformer secondary reactive burden, and current-transformer ratio. It is of particular interest to note that the presence of energized lumped capacitance electrically close to the particular capacitor circuit being switched will usually greatly accentuate the resulting current-transformer secondary voltage.

For quick reference, a tabulation of calculated current-transformer secondary potentials covering a fairly broad range of application conditions has been included in Appendix B. Voltage values to be expected when switching a single lumped capacitance electrically remote from energized lumped capacitance appear in Table I and Figure 8. Calculated voltage magnitudes for the case in which the electrical system contains nearby energized lumped capacitance appear in Table II and Figure 9. It is the purpose of the figure in both instances to portray graphically the relative effect of variation in the predominant influencing factors.

Table I. Calculated Current-Transformer Secondary Voltage e_S With One-Ohm Reactive Burden

Case 1. Single Circuit Containing Lumped Capacitance*



Current-Transformer Primary Amperes	System Short-Circuit Kilovolt-Amperes	Primary Operating Potential (E_L) Volts: 13,800	6,900	4,160	2,400
1,200	500,000	87	175		
600		174	348		
300		347	689		
150		684	1,320		
50		1,820	2,750		
1,200	250,000	44	88	145	
600		87	174	289	
300		174	347	574	
150		346	680	1,115	
50		974	1,665	2,560	
1,200	150,000		52	87	150
600			105	173	298
300			208	345	590
150			411	680	1,123
50			1,090	1,710	2,180
1,200	75,000		26	43	75
600			52	86	149
300			105	173	297
150			208	341	581
50			583	938	1,360
1,200	50,000				51
600					100
300					199
150					392
50					990
Current-transformer voltage class		15,000	7,500	5,000	5,000

*These voltages will be produced under short-circuit conditions.

Fault-Current Flow

In the presence of primary-circuit fault-current flow, a corresponding current will flow in the current-transformer secondary winding, which in turn will produce a high current-transformer secondary voltage. Since in the previous case it is reasoned that a lumped capacitance initially appears as a system short circuit, it follows that the same reasoning may be used in judging probable voltage magnitudes in the case of fault-current flow. In general, however, the system impedance will be so large in proportion to that of the current transformer that the voltages produced are not excessive for the insulation of the secondary or the devices connected to it.

It is, however, possible that the presence of energized lumped capacitance may accentuate the resulting transient current-transformer secondary voltage, in the same manner as outlined under the topic of switching transients. It is probable that such accentuated over voltages would be realized only if the character of the electrical fault were such that the transition between a good insulator and a very good conductor was extremely abrupt, an example of which would be the closure of a bolted fault circuit by means of a power-circuit switch.

Test Verification

To establish the fact that excessive overvoltages could be generated in current-transformer secondary circuits in the manner described in this paper a test setup was made using facilities which could be made available for this purpose. The test circuit arrangement is defined in Figure 4. Closure of the circuit breaker in this test setup produced current-transformer secondary potentials, measured by a calibrated sphere gap, of 9,750 volts, with a reactive burden of 2.96-ohms and 5,150 volts with a reactive burden of 1.74 ohms.

The circuit configuration unfortunately does not simulate closely that encountered in practical circuit operation and is therefore not suited for checking quantitative agreements with theoretical analysis. The tests do serve to establish the following points:

1. Excessive current-transformer voltages may be generated in the manner described in this paper.

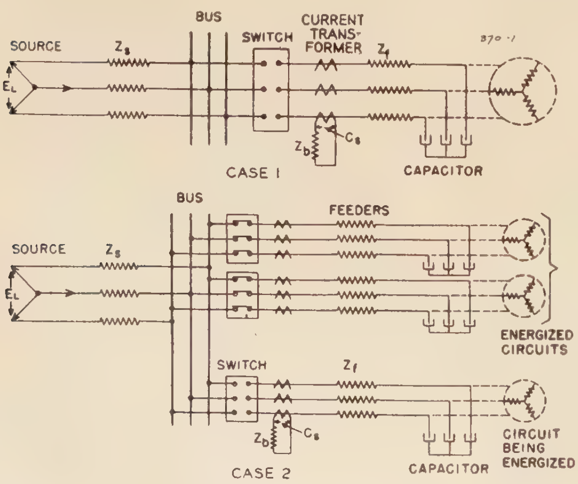


Figure 1 (left). Fundamental circuit arrangements which give rise to high switching transient voltages in current-transformer secondary circuits

Case 1. Single circuit containing lumped capacitance

Case 2. Multiple circuits containing lumped capacitance

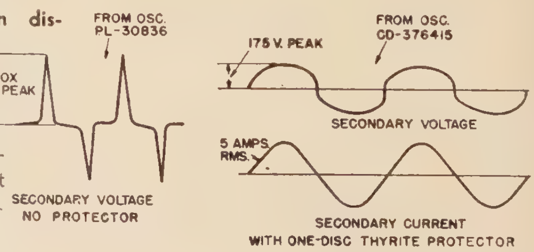


Figure 2 (right). Voltage oscillograms of current-transformer secondary voltage with burden disconnected

Case 1. Secondary voltage, no protector

Case 2. Secondary voltage and secondary current with one disk thyrite protector

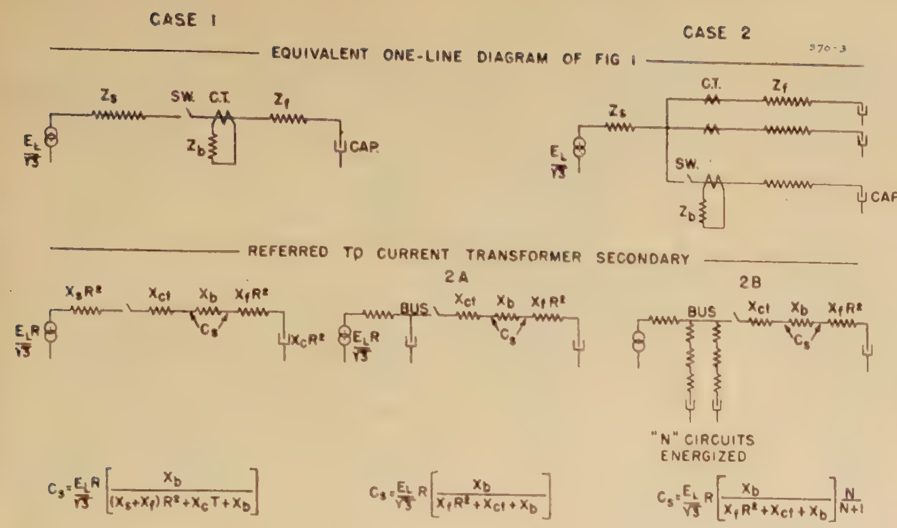


Figure 3. Equivalent-circuit diagram corresponding to Figure 1

2. The generation of such excessive voltages is not contingent on the presence of a high capacity power supply system.

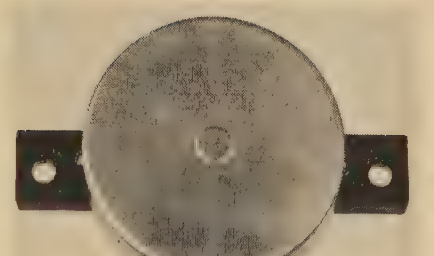
3. Variation in the physical size of capacitor units does not significantly change the magnitude of secondary voltage generated.

Need for Overvoltage Protection

Unless restricted in a suitable manner, in applications involving lumped capacitance, excessive voltages may appear in current-transformer secondary circuits which are dangerous to current-transformer and secondary-circuit insulation and to operating personnel. Secondary-circuit insulation failure assumes particular importance in the case of circuits containing current-actuated protective relays which may be rendered inoperative. Secondary-circuit insulation which has been punctured by excessive transient voltage may not be evident at normal rated current levels but may fail completely when subjected to increased voltages attendant with fault current flow. There is, therefore, a distinct need for an effective reliable overvoltage protector for current transformer circuits.

Protective Equipment

Many overvoltage protective devices which have been applied to current transformer secondary circuits, such as film cutouts, vacuum-tube devices, restricted core designs, and secondary filters, leave much to be desired with respect to initial performance, reliability, simplicity, and application flexibility. In view of the fact that unprotected current-transformer circuits commonly may be subject to recurrent transient overvoltages, it is evident that an adequate protective device must be capable of repetitive operation without change in characteristics, small, compact, and easy to apply, and at the same time be low in first cost.



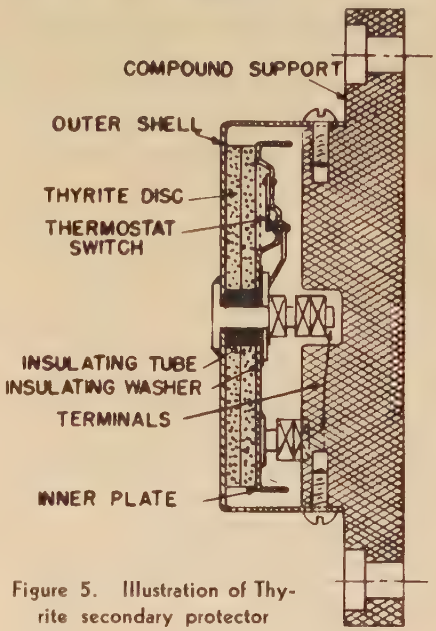


Figure 5. Illustration of Thyrite secondary protector

Table A

Burden	Per Cent Normal Current	Test Without Protector: Ratio	Test Without Protector: Phase Angle	Test With Protector (One Disk): Ratio	Test With Protector (One Disk): Phase Angle
Z (50 volt-amperes at 50 per cent power factor)	10	1.0045	+10	1.0046	+10
	20	1.0025	+ 5	1.0027	+ 5
	40	1.0005	− 2	1.0006	− 3
	60	1.0000	− 4	1.0002	− 5
	100	1.0017	+ 2	1.0019	+ 1
	150	1.0033	+ 5	1.0036	+ 4
	200	1.0043	+ 7	1.0048	+ 5

New Overvoltage Protector

A secondary protector has been developed which meets these requirements. The complete unit is illustrated in Figure 5. The active element embodies one or two disks made of material having nonlinear resistance characteristics and of such character as to secure adequate control of overvoltages, yet avoid objectionable error in secondary currents throughout the operating overcurrent range.

To avoid excessive temperature of the disks in applications which may be subject to secondary open circuit, an auxiliary thermostatic switch is used which short-

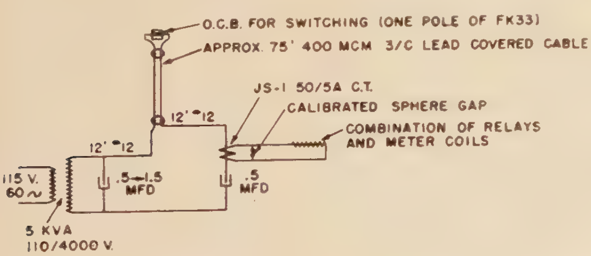


Figure 4. Diagram of test circuit used to verify production of high switching-transient voltages

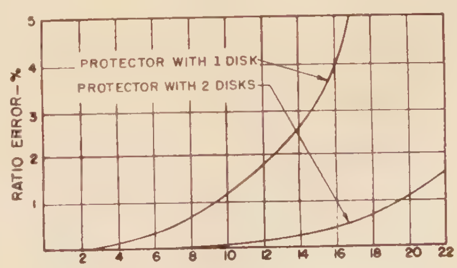


Figure 6. Ratio error caused by Thyrite protector at high currents

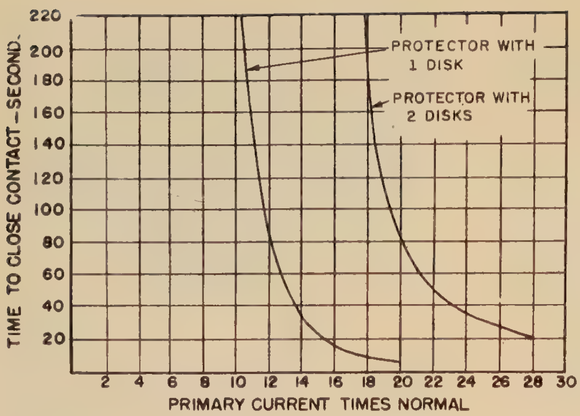


Figure 7. Time to close thermostatic switch contacts at high currents with a Z burden

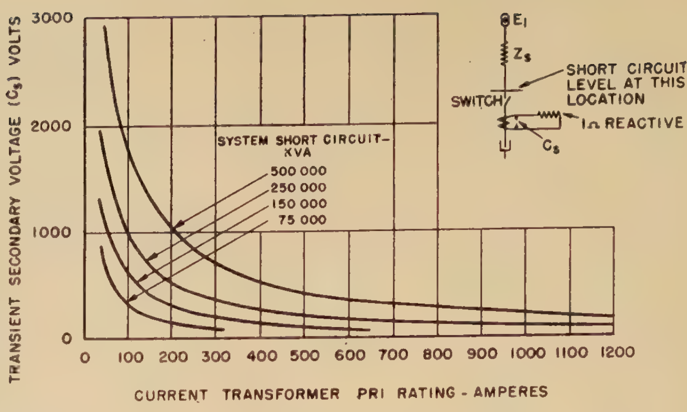


Figure 8. Curve of calculated results applying to case 1 for 6,600-volt operation

Case 1. Single circuit containing lumped capacitance

E_1—6,600-volt three-phase 60 cycles

Current transformer—Instrument-type, five-ampere secondary winding

X_b—One ohm

circuits the active unit upon reaching a temperature of approximately 100 degrees centigrade and automatically resets as the temperature of the disks drops below the assigned maximum level.

The protector is built in two forms—one with a single disk and the other with two such disks in series.

The two-disk unit is suitable for general application and applicable to current-transformer circuits used for relaying, as well as metering. The single-disk unit is designed for the very minimum secondary overvoltage and accepts a sacrifice in accuracy at high currents. It is intended to be used on current-transformer circuits for metering only. The single-disk unit is particularly adapted to operating conditions in which operating personnel frequently contact portions of the current-transformer secondary circuit and current-transformer circuits may easily be inadvertently open-circuited.

Pertinent performance characteristics of the secondary protector are as follows:

Overvoltage

With rated current flowing in the current-transformer primary winding and the secondary burden disconnected, the protector will limit the secondary voltage to the following:

	Rms Volts	Peak Volts
One-disk unit	135	175
Two-disk unit	270	350

Figure 2 portrays the wave shape of the resulting secondary voltage with the normal secondary burden entirely removed. The character of the secondary voltage with the protector is in sharp contrast with that which would be obtained without protector.

Secondary-Circuit Accuracy

The effect of either protector on phase angle and ratio accuracy with a Z burden (50 volt-amperes at 0.5 power factor) is negligible under normal operating conditions between 5 per cent and 200 per cent of rated load current as evidenced by the test information in Table A.

At high currents, the ratio accuracy of the current transformer with the protector connected across its secondary terminals and with a Z burden is shown in Figure 6. Substantial ratio error occurs with currents of 15 times normal and higher with the single-disk protector, which may be objectionable for relaying service.

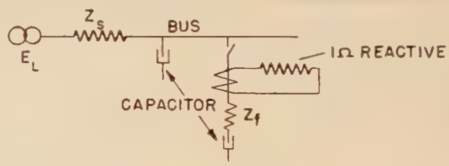
Thermal-Switch Action

The thermal switch is needed only for the purpose of limiting the temperature to a safe value in the presence of an open secondary circuit. Under normal operation, including the temporary flow of fault current, excessive temperature will not be realized and a thermal protective switch if present would not operate. Figure 7 defines the time required for the thermal switch contacts to close as a function of current magnitude, again based on the presence of a Z secondary burden.

The recycling characteristic of the protector with thermal switch in the presence

Table II. Calculated Current-Transformer Secondary Voltage e_S With One-Ohm Reactive Burden

Case 2. Multiple Circuits Containing Lumped Capacitance



Z_f	Current-Transformer Primary Amperes	Primary Operating Potential—Volts					
		13,200	6,600	4,000	2,300	400	220
0.03 ohm per phase Approximately 500 feet Three-conductor cable	1,200	1,053	527	319	183	35	18
	600	2,090	1,045	640	368	70	35
	300	4,040	2,020	1,250	721	138	69
	200	5,770	2,890	1,840	1,060	203	101
	100	8,940	4,460	3,170	1,830	350	233
	50	9,530	4,770	4,200	2,420	462	232
0.012 ohm per phase Approximately 200 feet Three-conductor cable	1,200	2,620	1,310	795	458	88	44
	600	5,130	2,560	1,575	905	173	87
	300	9,460	4,730	3,030	1,740	334	167
	200	12,600	6,300	4,270	2,450	470	234
	100	15,500	7,750	6,310	3,620	695	346
	50	12,300	6,150	6,230	3,590	690	345
0.006 ohm per phase Approximately 100 feet Three-conductor cable	1,200	5,180	2,590	1,585	912	175	88
	600	9,950	4,980	3,100	1,785	342	171
	300	17,200	8,590	5,750	3,300	632	317
	200	20,900	10,470	7,650	4,400	842	420
	150	22,000	11,020	8,770	5,050	965	482
	100	20,550	10,290	9,400	5,410	1,035	518
	50	13,650	6,810	7,460	4,280	822	410
0.003 ohm per phase Approximately 50 feet Three-conductor cable	1,200	10,250	5,130	3,150	1,810	347	173
	600	19,080	9,550	6,100	3,500	671	335
	300	28,900	14,450	10,400	5,970	1,140	574
	200	31,200	15,600	12,650	7,300	1,400	698
	100	24,600	12,300	12,500	7,180	1,375	686
	50	14,400	7,200	8,250	4,740	910	455
Current-transformer voltage class		15,000	7,500	5,000	5,000	5,000	5,000

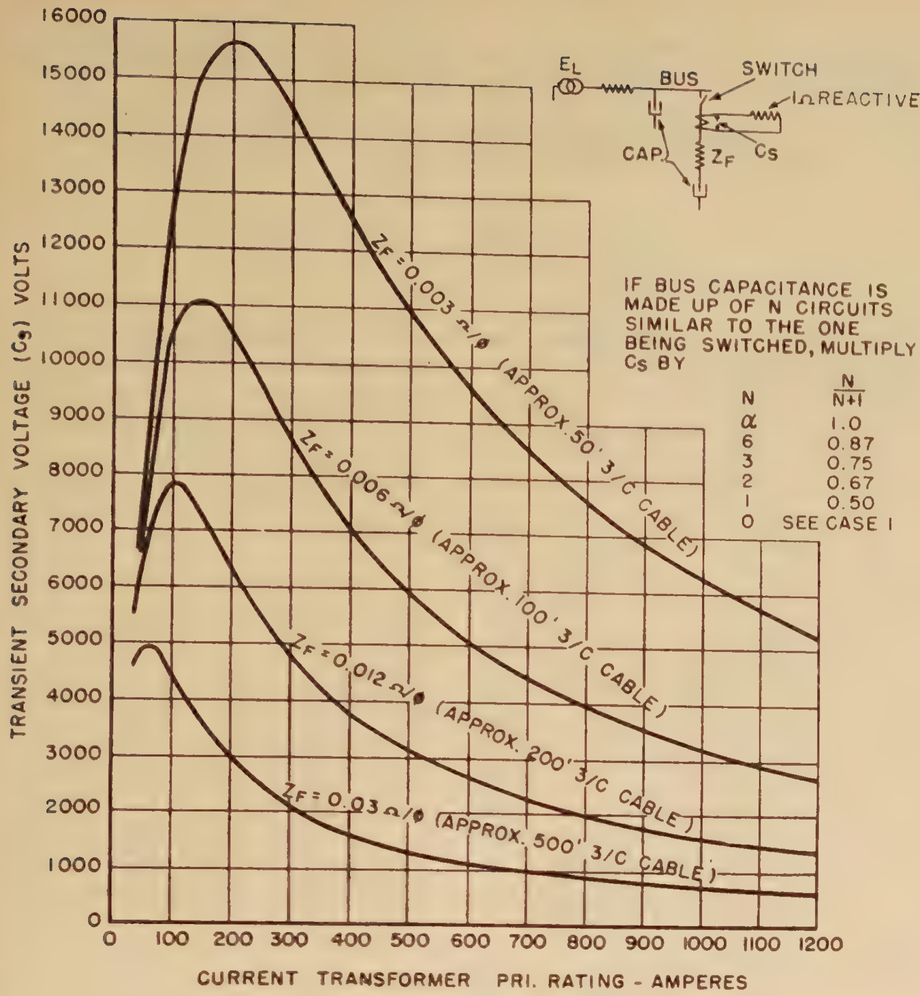


Figure 9. Curve of calculated results applying to case 2 for 6,600-volt operation

E_L—6,600-volt three-phase 60 cycles

Current transformer—Instrument-type, five-ampere secondary winding

X_B—One ohm

of a permanent open-circuited secondary winding and normal rated current flowing in the primary winding is:

	Time to Close Contacts	Time to Reset
One-disk unit	10 seconds	8 minutes
Two-disk unit	14 seconds	11 minutes

Outstanding Advantages

The new secondary protector constitutes a simple, reliable, inexpensive, overvoltage device for general application. It is small, compact, and suitable for direct mounting on the current-transformer structure. The outstanding features are:

1. Permanence of characteristics—not influenced by repetitive operation.

2. Effective voltage protection provided irrespective of the voltage source or the possible oscillation frequencies involved.

3. No operating time lag.

4. Negligible influence on accuracy within the normal range of operating current and burden.

5. Simple, compact, and applicable to existing as well as new current-transformer installations.

6. Active element completely enclosed—mechanical protection provided and dust and other foreign materials excluded.

Appendix A. Current-Transformer Switching-Transient Overvoltage Circuit Analysis

The fundamental circuit arrangements illustrated in Figure 1 are first reduced to an equivalent line to neutral one-line diagram as shown on the upper part of Figure 3.

Refer to Current-Transformer Secondary Winding

A better physical conception of circuit behavior will be gained through analysis in terms of the current-transformer secondary winding. In referring primary circuit quantities to the secondary, voltages are multiplied by R and impedances by R^2. Current-transformer internal impedance is generally available expressed in terms of the secondary winding.

The effective primary circuit voltage is taken as $E_L/\sqrt{3}$ which would correspond to simultaneous closing of the three poles of the switching unit. It might be reasoned that the transient would be controlled by the first two poles to close, in which case the effective primary circuit voltage would be $E_L/2$. As there is no significant difference between the two ($0.577E_L$ and $0.5E_L$), only one (the more pessimistic) will be used.

The resulting equivalent circuits referred to the secondary winding appear in the lower portion of Figure 3.

Evaluation of Secondary Voltage

By neglecting circuit resistance, a tremendous reduction in complexity of analysis is effected. That it is reasonable to do so is judged by the following:

1. At fundamental frequency, resistance generally plays only a minor part in influencing voltage drop through circuit impedance elements.

2. Invariably switching transients which will give rise to dangerous secondary voltages will be associated with oscillating frequencies far above normal which further reduce the significance of resistance.

3. A high degree of accuracy in secondary-voltage evaluation is of slight import. Simplicity in analysis is far more important.

Immediately following circuit closure, system voltage becomes distributed along the inductive reactance between the voltage source and the de-energized capacitor switched on the line. That portion which appears across the current-transformer total secondary burden X_B represents the voltage which appears at the secondary terminals.

Case 1. A single circuit containing lumped capacitance. The expression for current-transformer secondary voltage immediately following switch closure can be directly written.

$$e_S=\frac{E_L}{\sqrt{3}}R\left[\frac{X_B}{(X_s+X_f)R^2+X_{CT}+X_B}\right]$$

Case 2. Multiple circuits containing lumped capacitance. The treatment of this case will be treated in two steps, *A* and *B*. Case 2*A* assumes the presence of lumped capacitance directly at the distribution bus, while case 2*B* considers a number of similar feeder circuits each containing lumped capacitance, only one of which is de-energized and to be switched on the line.

It will be apparent that an energized capacitor will resist a change in its terminal voltage in the same manner as a de-energized unit. The initial transient distribution of system potential will thus occur across circuit impedance elements intervening between the energized and the de-energized capacitance blocks.

Case 2A. With lumped capacitance at the distribution bus, the supply system impedance Z_S is effectively shunted out of the transient oscillating circuit. The expression for current-transformer secondary voltage becomes

$$e_S=\frac{E_L}{\sqrt{3}}R\left[\frac{X_B}{X_fR^2+X_{CT}+X_B}\right]$$

Case 2B. The secondary voltage, e_S, when switching one deenergized circuit in the presence of N similar circuits which already energized from the same bus, can be expressed as a fraction of the voltage which would result in case 2*A*. A certain portion of the total system voltage will appear across inductive reactance elements contained in the energized circuits, which

portion will diminish as the number of energized circuits increases. With N energized circuits, the switching of a similar circuit will result in a secondary voltage on the switched circuit of—

$$e_S = \frac{E_L}{\sqrt{3}} R\left[\frac{X_B}{X_f R^2 + X_{CT} + X_B}\right] \times \frac{N}{N+1}$$

$$= e_S \text{ (case } 2A) \times \frac{N}{N+1}$$

Values for the modifier $\frac{N}{N+1}$ are:

N	$\frac{N}{N+1}$
Infinite	1.0
6	0.87
3	0.75
2	0.67
1	0.5
0	See case 1

Nomenclature

Z_S—Primary supply circuit impedance r_s+jX_s ohms per phase (Y)

Z_f—Primary feeder circuit impedance r_f+jX_f ohms per phase (Y)

Z_c—Primary capacitance block impedance $-jX_c$ ohms per phase (Y)

Z_{CT}—Current-transformer internal impedance $r_{CT}+jX_{CT}$ ohms (referred to secondary)

Z_B—Current-transformer secondary burden impedance r_B+jX_B ohms

E_L—Primary circuit-potential—line-to-line volts

e_S—Current-transformer secondary terminal voltage

R—Current-transformer ratio.

Appendix B. Current-Transformer Switching-Transient Overvoltage Calculated Results

For quick estimating purposes, calculations have been made using the methods outlined in Appendix A, covering a fairly broad range of application conditions both for case A and case B.

All calculated results apply to a five-ampere rated secondary current.

All calculated results are based on a secondary reactive burden of one ohm. For practical purposes, the resulting secondary voltage may be considered directly proportional to X_B.

All results apply expressly to 60-cycle operation. The switching transient is independent of operating frequency, and results here tabulated may be used for other operating frequencies if carefully converted.

Results included under case 1 may be used to judge the secondary voltage magnitude resulting from fault-current flow in a system free of near-by lumped capacitance.

Specific Results

CASE 1

Table I. Tabulated transient secondary voltage for current-transformer primary current ratings ranging from 50 to 1,200 amperes, operating voltages of 13,800, 6,900, 4,160, and 2,400 volts, short-circuit kilovolt-ampere levels from 50,000 to 500,000-kva.

Figure 8. Calculated values for 6,900-volt operation plotted in curve form to illustrate the influence of various factors.

CASE 2

Table II. Tabulated transient secondary voltage of the same character as in Table I.

Figure 9. Values for 6,900-volt operation plotted in curve form to illustrate the influence of various factors.

Typical Circuit Constants

Current-transformer internal series impedance (instrument type):

5,000 volts class $1.+j1.5$ ohms (60 cycles) referred to five amperes secondary windings

7,500 volts class $1.3+j4.0$ ohms (60 cycles) referred to five amperes secondary windings

15,000 volts class $1.3+j4.7$ ohms (60 cycles) referred to five amperes secondary windings

Secondary burden (devices—60 cycles):

Ammeter wattmeter watt-hour meter (*AD*-7 five amperes) $0.05+j0.05$ ohm

Power-factor meter (*AD*-7) $0.11+j0.11$ ohm

Recording ammeter (*CD*-3 five amperes) $0.12+j0.5$ ohm

IAC relay (4–15 amperes on 4-A tap) $0.14+j0.34$ ohm

IBC relay (4–15 amperes on 4-A tap) $0.2+j0.45$ ohm

Power-circuit cable reactance—60 cycles:

7,500 volts and less 0.026–0.033 ohm/1,000: number 1/0 and larger

15,000 volts 0.031–0.039 ohm/1,000: 500,000 cular mils—number 1/0

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G. F. LELAND
NONMEMBER AIEE

L. T. RADER
ASSOCIATE AIEE

WITH high-production machines requiring rapid-duty cycling and accurate positioning, the speed-time characteristics of accelerating and decelerating motors are very important. D-c shunt machines are often employed for these applications because of their flexibility both in starting and stopping. One method commonly employed to bring the rotating system to rest is to disconnect the motor from the line and reconnect it across a resistor. The shunt field remains connected to the line so that the motor acts as a generator driven by the stored energy in the load. This method is known as dynamic braking. The elementary connection of the dynamic-braking circuit is shown in Figure 1. Since the torque developed is proportional to the product of field flux and armature current, and the latter depends on the speed, the braking effect obtained decreases as the motor loses speed. A friction-type load, independent of the speed, must be present in order to bring the motor to rest.

A method often used for calculating these accelerating and decelerating times is based on an average torque formula.[1]

$$T_{\text{average}} = \frac{(WR)^2\,(N)}{307.8\ (t \text{ seconds})}$$

This average torque formula rests on the assumption of constant angular acceleration and gives reasonably good results for systems where the frictional component of torque is large. However, it can lead to results which are greatly in error when the frictional component of retarding forces is small with respect to the full load torque of the motor in a system which has considerable stored energy.

Scope of Problem

This paper presents an analysis which leads to an effective and accurate method of calculating the performance of a d-c shunt motor under dynamic braking when connected to various types of load.

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G. F. LELAND and L. T. RADER are both in the industrial control engineering department, General Electric Company, Schenectady, N. Y.

The authors acknowledge the assistance of R. R. Lang in checking the equations and making calculations.

Three general solutions are covered depending on the type of load.

1. Torque varying directly as the speed together with a frictional component independent of the speed.

2. Torque varying as the square of the speed together with a frictional component independent of the speed, such as for a fan or blower operating with constant discharge orifices.

3. Torque independent of the speed such as a crane hoist operating against gravity, or a wire drawing machine.

This analysis is made only for the shunt motor with constant field strength at full field. The more common applications such as the variable speed d-c motors and the series d-c motors are not discussed.

Conventions and Assumptions

Most authors use the well-known equations $V=e+ir$ for a motor and $e=V+ir$ for a generator so that the direction of current is positive for either machine under normal operating conditions. In order to differentiate between motor current and generator current, subscripts must be used together with the relation that motor and generator current flow in opposite directions. This convention, predicated on the use of the above two equations is, however, unnecessary. It is also confusing when considering one machine which alternately acts as a motor or a generator. The difficulty is eliminated by using only the motor equation $V=e+ir$ together with the following conventions. Positive motor current causes positive motor torque. The direction of rotation of the machine when running as a motor is considered as the positive direction, so that positive torque and acceleration will cause an increase in speed. These relationships are shown in Figures 2 and 3.

The following assumptions are made:

1. Armature reaction is neglected. This assumption is justifiable since dynamic braking currents are usually limited to about 200 per cent full load current and armature reaction is negligible in this range.

2. Frictional forces are assumed constant throughout the entire speed range.

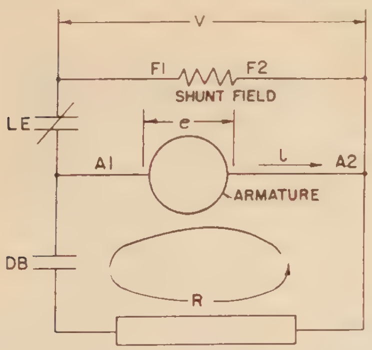


Figure 1. Elementary connection diagram showing dynamic braking circuit

LE—Line contactor
DB—Dynamic braking contactor
R—Total resistance in braking circuit

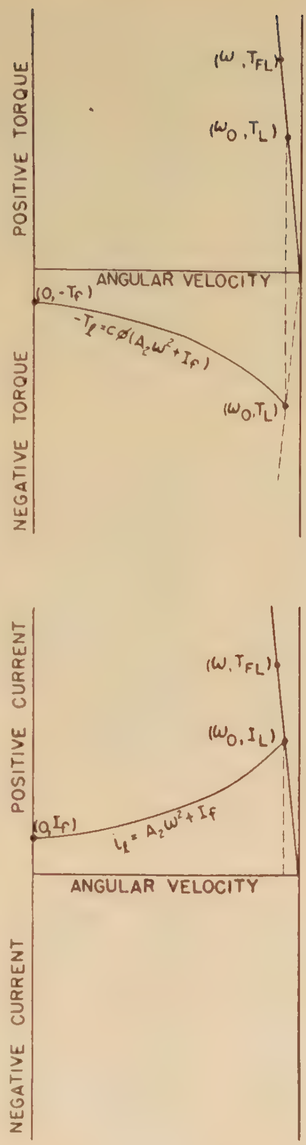
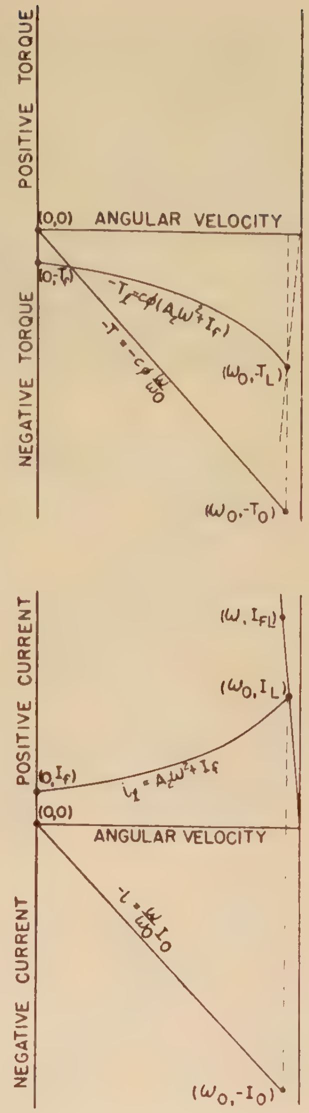


Figure 2 (left). Relations existing before dynamic braking between torque, current, and angular velocity when the load torque varies as the square of the speed

Figure 3 (right). Relations existing after dynamic braking between torque, current, and angular velocity when the load torque varies as the square of the speed



Results

The curves shown in Figures 4 to 8 give the performance of the shunt motor under dynamic braking for the assumed loads. Both coordinates are dimensionless quantities, so that the curves are applicable to any motor and system. The ordinate is the ratio of the current at any instant as a fraction of the initial braking current. When the value of the braking resistor remains unchanged, this ordinate is also a ratio of the speed at any instant to the initial speed. The abscissa is given as a function of the initial watts in the braking circuit, the total mechanical stored energy in the rotating system, and the time.

Although the curves are given for a constant braking resistance, they may be applied when the resistance is decreased in steps during the braking period, provided only that the speed at which the switching occurs is considered full speed, and the new KE_0 is then used.

The parameter, M, is the constant torque component of the armature current as a function of the full load current. It should be noted that when $M=1$, case *b*

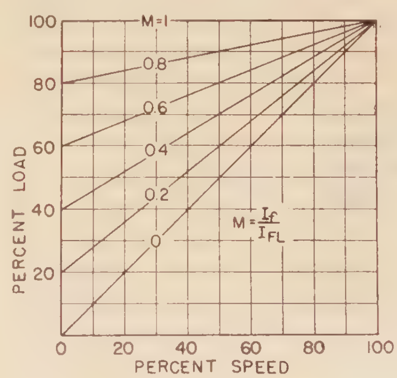


Figure 4. Load torque versus speed for various ratios of friction load to full load when the load torque varies as the first power of the speed

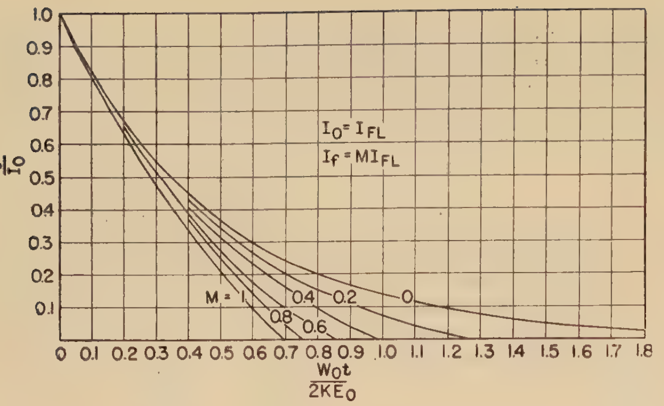


Figure 5a. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the first power of the speed, and the initial braking current is equal to the full load current

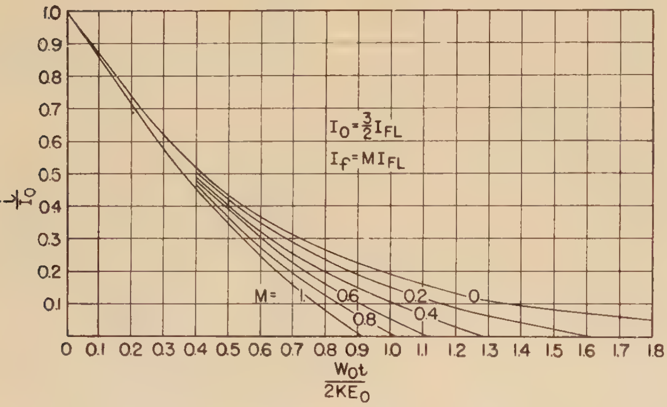


Figure 5b. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the first power of the speed, and the initial braking current is 150 per cent of full load current

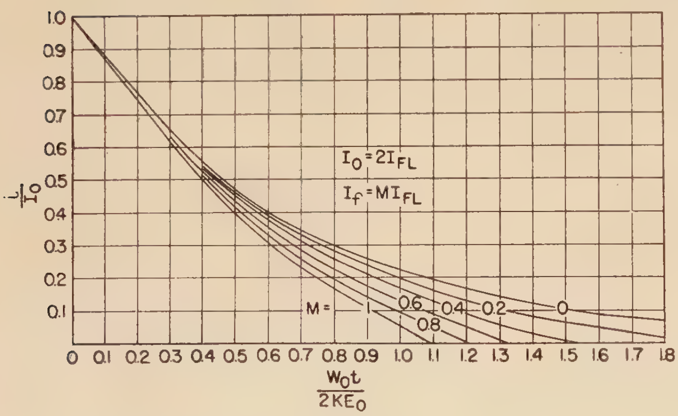


Figure 5c. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the first power of the speed, and the initial braking current is 200 per cent of full load current

and case c are identical and reduce to case a.

Using this method then it is possible to calculate the time taken by a system of known inertia and speed to come to rest; or conversely the required braking resistor required to bring the system to rest in a specified time. This method also furnishes a useful guide for the selection of motors with the correct moment of inertia when they must perform under severe duty cycles of starts and stops. It is interesting to note in this connection that a small motor will sometimes give a certain duty-cycle performance with a load where a larger motor would fail under the same conditions.

The cases treated cover many of the usual loads encountered in industrial work, but the curves can be extrapolated for other speed torque conditions, or the equations can be set up for any conditions and solved using graphical integration if necessary.

The curves presented for case b and case c are for dynamically braking a fully loaded motor. Similar curves can be drawn for any other load condition by assigning different values to b in equations 15 and 17.

Analysis

(For nomenclature see Appendix I.)

The equation for generated voltage is

$$e = \frac{ZP}{m} \frac{\phi n}{(60)10^8} \text{ volts,}^2 \text{ and since } \omega = \frac{2\pi n}{60}$$

$$e = \frac{c\phi\omega}{2\pi 10^8} \text{ volts} \tag{1}$$

where $c = ZP/m$. The torque equation[3] is

$$T = \frac{ZP}{m} \frac{\phi i}{20\pi} \text{ dyne-centimeters}$$

and since 10^7 dyne-centimeters = 1 watt-second, this becomes

$$T = \frac{ZP}{m} \frac{\phi i}{2\pi 10^8} = \frac{c\phi i}{2\pi 10^8} \text{ watt-seconds} \tag{2}$$

The load torque, which is negative, causes a positive armature current to flow (Figure 2), and since positive current has been defined as the current that produces a positive torque, then

$$T_L = \frac{-c\phi i_L}{2\pi 10^8} \text{ watt-seconds} \tag{3}$$

Now,

$$T + T_L = J\frac{d\omega}{dt} = J\alpha \tag{4}$$

At time $t = 0$, the line contactor LE shown in Figure 1 is opened and the dynamic braking contactor DB is closed, so that

$$e = -iR = \frac{c\phi\omega}{2\pi 10^8} \tag{5}$$

Since $KE = {}^1/_2 J\omega^2$ then

$$J = \frac{2KE}{\omega^2} = \frac{2KE_0}{\omega_0^2} \tag{6}$$

Combining equations 4 and 6 gives

$$T + T_L = \frac{2KE_0}{\omega_0^2} \frac{d\omega}{dt} \tag{7}$$

Expressing the torques in terms of currents, equation 7 becomes

$$\frac{c\phi i}{2\pi 10^8} - \frac{c\phi i_L}{2\pi 10^8} = \frac{2KE_0}{\omega_0^2} \frac{d\omega}{dt} \tag{8}$$

From equation 5

$$\omega = -\frac{iR2\pi 10^8}{c\phi} \tag{9}$$

and since $i = -I_0$ at $t = 0$ then

$$\omega_0 = \frac{I_0 R 2\pi 10^8}{c\phi} \tag{9a}$$

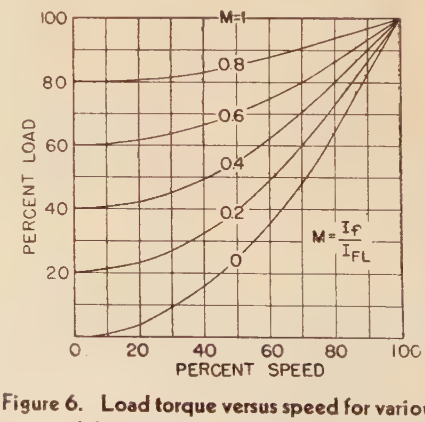


Figure 6. Load torque versus speed for various ratios of friction load to full load when the load torque varies as the square of the speed

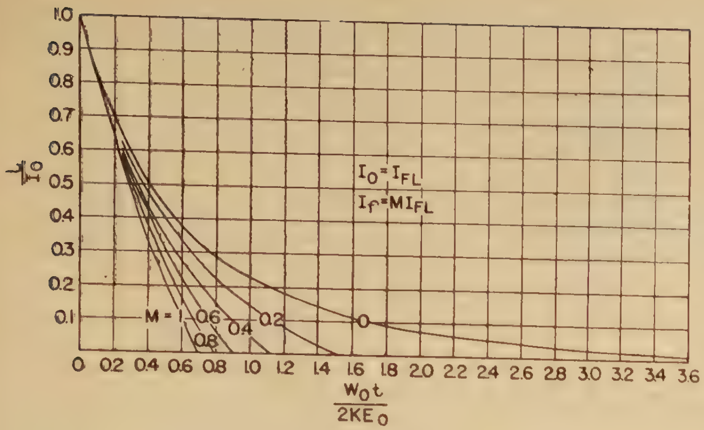


Figure 7a. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the square of the speed, and the initial braking current is equal to the full load current

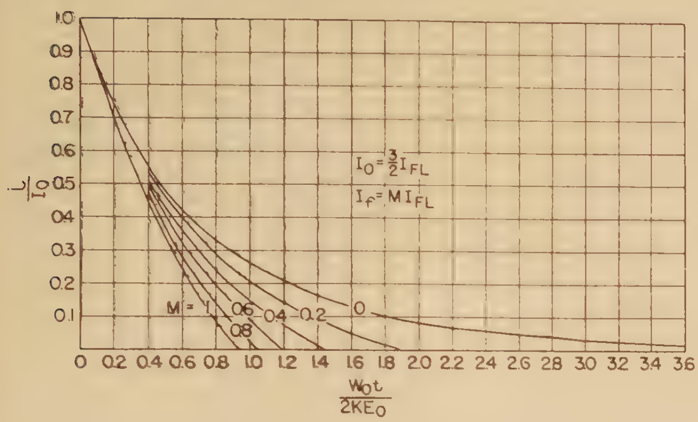


Figure 7b. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the square of the speed, and the initial braking current is 150 per cent of full load current

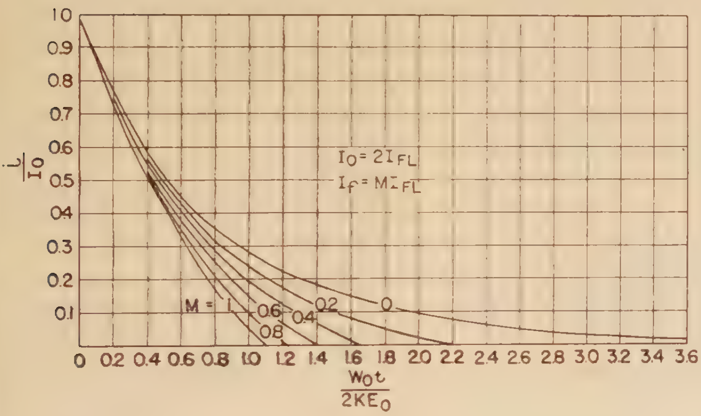


Figure 7c. Current and time relations during dynamic braking for various ratios of friction load to full load when the load torque varies as the square of the speed, and the initial braking current is 200 per cent of full load current

so that

$$\omega_0^2 = \left[\frac{I_0 R 2\pi 10^8}{c\phi}\right]^2 = W_0 R \left[\frac{2\pi 10^8}{c\phi}\right]^2$$

and

$$\frac{1}{\omega_0^2}\frac{d\omega}{dt} = \frac{-c\phi}{W_0 2\pi 10^8}\frac{di}{dt} \tag{10}$$

Substituting equation 10 in equation 8 and simplifying, the latter reduces to

$$i - i_L = \frac{-2KE_0}{W_0}\frac{di}{dt} \tag{11}$$

Equation 11 is the fundamental equation governing dynamic braking and can be solved when i_L is known as a function of speed.

Case I

$i_L = A_1\omega + I_f$

Immediately after switching, $i_L = I_L$, for the speed of $\omega = \omega_0$. Therefore, $I_L = A_1\omega_0 + I_f$. The load current can be expressed in terms of I_{FL} by letting $I_L = bI_{FL}$ where b is the ratio between the two currents.

Therefore, $A_1 = (bI_{FL} - I_f)/\omega_0$ so that

$$i_L = (bI_{FL} - I_f)\frac{\omega}{\omega_0} + I_f \tag{12}$$

After switching to the dynamic braking connection, the armature current at any speed is obtained from equations 9 and 9a yielding, $\omega/\omega_0 = -i/I_0$ so that equation 12 becomes

$$i_L = \left(\frac{I_f - bI_{FL}}{I_0}\right) i + I_f \tag{13}$$

Substituting equation 13 into equation 11 yields

$$\frac{2KE_0}{W_0}\frac{di}{dt} + \frac{(I_0 + bI_{FL} - I_f)}{I_0} i = I_f \tag{14}$$

This is a linear first-order first-degree differential equation whose solution, after substitution of the boundary condition, $i = -I_0$ at $t = 0$ is

$$\frac{i}{I_0} = \frac{I_f}{I_0 + bI_{FL} - I_f} - \left[\frac{I_f}{I_0 + bI_{FL} - I_f} + 1\right]\epsilon^{-\frac{W_0 t}{2KE_0}\left[\frac{I_0 + bI_{FL} - I_f}{I_0}\right]} \tag{15}$$

For the fully loaded motor b is unity, and the equation becomes

$$\frac{i}{I_0} = \frac{I_f}{I_0 + I_{FL} - I_f} - \left[\frac{I_f}{I_0 + I_{FL} - I_f} + 1\right]\epsilon^{-\frac{W_0 t}{2KE_0}\left[\frac{I_0 + I_{FL} - I_f}{I_0}\right]} \tag{15a}$$

Equation 15*a* is shown plotted in Figures 5*a*, 5*b*, and 5*c* for various percentage values of I_f to I_{FL}. The maximum value of I_0 is usually governed by the commutating ability of the machine. Two hundred per cent full load current is a representative value.

Case II

$i_L = A_2\omega^2 + I_f$

For conditions immediately after switching where $i_L = I_L = bI_{FL}$ this equation reduces to

$$i_L = \frac{(bI_{FL} - I_f)}{I_0^2} i^2 + I_f \tag{16}$$

Substituting equation 16 into equation 11 yields

$$\frac{2KE_0}{W_0}\frac{di}{dt} - \frac{(bI_{FL} - I_f)}{I_0^2} i^2 + i = I_f \tag{17}$$

Substituting b equal to unity, the solution of equation 17 is

$$\frac{W_0 t}{2KE_0} = I_0^2 \int \frac{di}{(I_{FL} - I_f)i^2 - I_0^2 i + I_0^2 I_f} + \text{constant} \tag{18}$$

The right-hand side of this equation is of the form $\int(dx/X)$ where

$$X = ax^2 + bx + c \tag{19}$$

and the solution[4] depends on the value of the discriminant $b^2 - 4ac$. This term is

$$I_0^2[I_0^2 - 4I_f(I_{FL} - I_f)] \tag{20}$$

Since I_{FL} is a constant, the maximum value of the second term in equation 20 is obtained by differentiating it with respect to I_f which yields $I_f = {}^1/_2 I_{FL}$. Equation 20 then becomes

$$I_0^2[I_0^2 - I_{FL}^2] \tag{21}$$

Considering only the case where $I_0 \geqq I_{FL}$

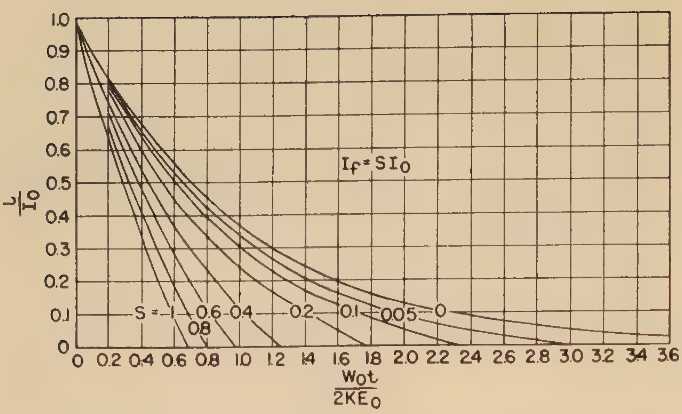


Figure 8. Current and time relations during dynamic braking for various ratios of friction load current to the initial braking current when the load torque is independent of speed

so that the discriminant is positive, the solution of equation 18, after substituting the boundary condition that $i=-I_0$ at $t=0$, is

$$\frac{W_0t}{2KE_0}=\frac{I_0}{B}\log\left[\frac{2(I_{FL}-I_f)i/I_0-I_0-B}{2(I_{FL}-I_f)i/I_0-I_0+B}\right]\times\left[\frac{2(I_{FL}-I_f)+I_0-B}{2(I_{FL}-I_f)+I_0+B}\right] \tag{22}$$

where

$$B=\sqrt{I_0^2-4I_fI_{FL}+4I_f^2}$$

Letting $I_f=MI_{FL}$ and $I_{FL}=I_0$ equation 22 reduces to

$$\frac{W_0t}{2KE_0}=\frac{1}{(2M-1)}\log\left[\frac{2(1-M)i/I_0-2M}{i/I_0-1}\right] \tag{23}$$

which is plotted in Figure 7*a* for various values of M. Substitution of $I_f=MI_{FL}$ and $I_0=3/2\ I_{FL}$ into equation 22 gives

$$\frac{W_0t}{2KE_0}=\frac{3}{\sqrt{16M^2-16M+9}}\times\log\left[\frac{(4M-7+\sqrt{16M^2-16M+9})i/I_0+4M+3+\sqrt{16M^2-16M+9}}{(4M-7-\sqrt{16M^2-16M+9})i/I_0+4M+3-\sqrt{16M^2-16M+9}}\right] \tag{24}$$

which is plotted in Figure 7*b* for various values of M. Substitution of $I_f=MI_{FL}$ and $I_0=2I_{FL}$ into equation 22 gives

$$\frac{W_0t}{2KE_0}=\frac{1}{\sqrt{M-M^2+1}}\times\log\left[\frac{(M-2+\sqrt{M^2-M+1})i/I_0+M+1+\sqrt{M^2-M+1}}{(M-2-\sqrt{M^2-M+1})i/I_0+M+1-\sqrt{M^2-M+1}}\right] \tag{25}$$

which is plotted in Figure 7*c* for various values of M.

Case III

$i_L=i_f$

For this constant-torque load condition, equation 11 reduces to

$$i-I_f=\frac{-2KE_0}{W_0}\frac{di}{dt} \tag{26}$$

Integration of equation 26 together with the boundary condition of $i=-I_0$ at $t=0$ gives the solution

$$\frac{i}{I_0}=\frac{I_f}{I_0}-\left(\frac{I_f}{I_0}+1\right)\epsilon^{-\frac{W_0t}{2KE_0}} \tag{27}$$

which is plotted in Figure 8 for various values of S, where S is the ratio I_f to I_0. This same solution can be obtained by substituting $bI_{FL}=I_f$ in equation 15.

Example

A Thearle balancing set carries a smooth rotor with a WR^2 of 2,400 pound-feet squared. This is driven by a 20-horsepower 230-volt 500-rpm 75-ampere full-load-current motor, with a WR^2 of 21 pound-feet squared. With a dynamic braking resistor of 3.78 ohms, it is required to find the time for the system to come to rest.

Motor speed at test = 584 rpm

WR^2 of test rotor referred to motor shaft = 552 pound-feet squared

WR^2 of two pulleys referred to motor shaft = two pound-feet squared

WR^2 of motor rotor = 21 pound-feet squared

Total WR^2 referred to motor shaft = 575 pound-feet squared

Internal resistance of motor $r=0.3$ ohms

Therefore, total dynamic braking resistor $R=4.08$ ohms so that $I_0=230/4.08=56.4$ amperes.

The steady-state load on the motor is 4.5 amperes and the speed is low enough so that windage can be considered negligible. Then

$$\frac{I_f}{I_0}=\frac{4.5}{56.4}=0.08$$

$W_0=I_0^2R=(56.4)^2(4.08)=12{,}900$ watts

$KE_0=(2.31)WR^2N^2 10^{-4}$ watt-seconds

$=(2.31)(575)(584)^2 10^{-4}=45{,}200$ watt-seconds

Therefore,

$$\frac{W_0t}{2KE_0}=0.143t$$

Using equation 27 and putting $i=0$ for the standstill condition

$$0=0.08-(0.08+1)\epsilon^{-0.143t}$$

from which

$t=18.2$ seconds

The same result may be obtained by interpolation from Figure 8.

Appendix

Nomenclature

A_1, A_2—Constants.
b—Ratio of I_L to I_{FL}.
c—ZP/m, motor constant.
DB—Dynamic braking contactor.
e—Counter *emf* at any instant.
i—Armature current at any instant.
i_L—Current required to supply the torque demanded by the load and equal I_f at $\omega=0$.
I_f—Current required to supply the torque demanded by the frictional component of the load.
I_L—Value of i_L at time $t=0$.
I_{FL}—Rated motor current.
I_0—Value of i at time $t=0$.
J—Polar moment of inertia of all Rotating parts in centimeter-gram-second units.
KE—Kinetic energy of rotating system in centimeter-gram-second units.
KE_0—2.31 (WR^2) $(N)^2$ 10^{-4} watt-seconds, value of KE at time $t=0$.
LE—Line contactor.
m—number of conductor groups connected in parallel between brushes in the motor.
M—Ratio of I_f to I_{FL}.
n—Speed in revolutions per minute.
N—Value of n at time $t=0$.
P—Number of magnetic poles in the motor.
r—Internal resistance in ohms.
R—Total ohms resistance in dynamic braking circuit.
S—Ratio of I_f to I_0.
t—Time in seconds measured from the time the dynamic braking contactor closes.
T—Developed torque in watt-seconds.
T_L—Load torque in watt-seconds.
V—Line voltage.
W_0—I_0^2R in watts.
WR^2—Inertia in pound-feet squared.
Z—Number of armature conductors.
α—Angular acceleration in radians per second per second.
ϕ—Useful flux per pole crossing the air gap.
ω—Angular velocity in radians per second.
ω_0—Value of ω at time $t=0$.

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Use of Equivalent Annual Ambient Temperature in Overloading Transformers and Voltage Regulators

M. S. OLDACRE
MEMBER AIEE

Synopsis: In the "Interim Report on Guides for Overloading Transformers and Voltage Regulators,"[1] reference is made to the use of equivalent annual ambient temperatures in determining the loading of transformer equipment. This paper is to show a method of calculating the equivalent annual ambient temperature from readily available climatological data to avoid difficulties in other methods that have been proposed. An outline is given also of conditions under which the equivalent annual ambient temperature may be applied in determining permissible overloads for transformers.

It has long been understood that the life of a transformer is dependent on the operating temperature of its insulation. A great amount of experimental work on aging of insulation has been done in recent years.[2–6] Although there has been no complete agreement on the time-temperature-aging characteristics of transformer insulation, the eight-degree-centigrade rule formulated by V. M. Montsinger has been given general acceptance.

The proposed American Standard *C*-57[7] recommends 95 degrees centigrade as the hottest-spot temperature that will give normal life for continuous operation of a transformer. For rating purposes this is restated as follows:

". . . transformers of usual design having a winding temperature rise (by resistance) of 55C at rated load . . . may be operated continuously at rated load . . . provided that . . . the temperature of the cooling air at no time exceeds 40C and the average temperature of the cooling air during any 24-hour period does not exceed 30C."

For the average transformer, an increase of one per cent in load above 100 per cent increases the hottest-spot temperature rise approximately one degree centigrade. Operation above 100 per cent load at ambient temperatures below 30 degrees centigrade will still not cause the hottest-spot temperature to exceed the limit of 95 degrees centigrade if the following American Standard rule is observed:

"Oil-immersed self-cooled transformers may be loaded continuously one per cent above rated kilovolt-amperes for each degree centigrade that the daily average temperature of the cooling medium (air) is below 30C."

In June 1942, the transformer subcommittee of the committee on electrical machinery issued an "Interim Report on Guides for Overloading Transformers and Voltage Regulators." This report gave a rule for overloading based on low ambient temperatures which included the following statement:

"Under some conditions greater permissible overload capability can be obtained by using 'equivalent annual ambient' instead of the daily average ambient when applying the rule for overloads due to change in ambient. The equivalent annual ambient is the temperature which, if maintained continuously, would result in the same aging as that occurring under the actual ambient temperature throughout the year."

The use of equivalent annual ambient temperature instead of average annual temperature takes into consideration the fact that the average rate of deterioration of insulation for a temperature range is higher than the rate of deterioration for the average temperature in the same range. This is in accordance with the eight-degree-centigrade rule mentioned above.

To calculate an equivalent annual ambient temperature would require the averaging of the aging for all the different ambient temperatures throughout the year. For general use this would have to be done for a period of years in order to arrive at a usable mean condition. Such lengthy calculations have been the chief difficulty in the application of equivalent ambient temperature. This paper presents an approximate method of determining equivalent annual ambient temperature from readily available United States Weather Bureau data. The results are compared with those obtained from complete summaries for a few cities to justify the general use of the approximate method.

Use of Climatological Data

Based on climatological data issued by the United States Weather Bureau, the range of ambient temperature during the year may be divided into three parts, as follows:

1. *Range during the year of monthly mean temperatures.* These are summarized nationally and are readily available.

2. *Range during the month of daily mean temperatures.* These are not summarized nationally, except on a normal basis, but are available locally from annual reports.

3. *Range during the day of hourly temperatures.* These are available nationally as the difference between the normal daily maximum and the normal daily minimum temperatures.

The daily mean temperature is the average of the maximum and minimum temperatures for the day, not the weighted average. The monthly mean temperature is an average of the daily mean temperatures during the month.

For use in calculating equivalent annual ambient temperature, there has been general agreement on the use of items 1 and 2 in the preceding list. There has been disagreement on the use of item 3 on two points, as follows:

(*a*). Present American Standard *C*-57 states: "A transformer may be operated continuously at rated load . . . provided that . . . the temperature of the cooling air at no time exceeds 40C, and the average temperature of the cooling air during any 24-hour period does not exceed 30C." To

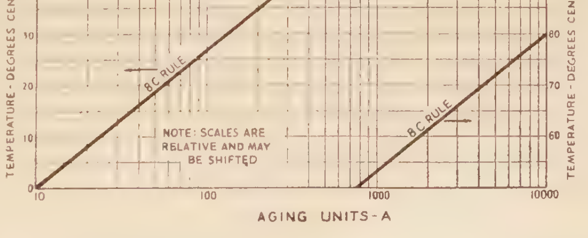

Figure 1. Effect of temperature on aging of transformer insulation

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M. S. OLDACRE is equipment and research engineer of the Commonwealth Edison Company, Chicago, Ill.

consider equivalent daily temperature instead of average daily temperature is contrary to this provision of the American Standard.

(*b*). On a daily temperature cycle the thermal lag of the transformer flattens out the range of hot-spot temperatures to make it less than the range of ambient temperatures.

For these reasons it is believed that the range during the day of hourly temperatures should be ignored in calculating equivalent annual ambient temperature and only items 1 and 2 considered. Furthermore, it was found that the use of item 3 affected the calculated value of equivalent annual ambient temperature by only a small fraction of a degree.

Calculation of Equivalent Ambient Temperature—Direct Method

Accepting as valid the eight-degree-centigrade law formulated by V. M. Montsinger,[2] the life of transformer insulation is halved for every eight-degree-centigrade increase in temperature and, conversely, is doubled for every eight-degree-centigrade decrease. This is expressed by the equation

$$L=K\epsilon^{-0.0865T} \qquad (1)$$

where

L = life of the transformer insulation
T = temperature in degrees centigrade
K = constant chosen to give rated life at rated temperature

Conversely, the rate of aging of transformer insulation per unit time of operation may be expressed by the equation

$$A=K_1\epsilon^{0.0865T} \qquad (2)$$

where

A = rate of aging or aging units consumed per unit time of operation
T = temperature in degrees centigrade
K_1 = a constant chosen to give rated life at rated temperature

For convenient use with climatological

Table I. Calculation of Equivalent Ambient Temperature for Kansas City, Mo.

(Considers Only the Monthly Mean Temperature)

Month	Monthly Average Temperature: Centigrade (2)	Monthly Average Temperature: Fahrenheit (3)	$\epsilon^{0.0865T}$ Centigrade (4)	$\epsilon^{0.0481T}$ Fahrenheit (5)	A_C Figure 1 (6)	A_F Figure 2 (7)
January	−1.3	29.7	0.89	4.17	9	42
February	−0.7	30.8	0.94	4.40	10	44
March	6.2	43.2	1.71	8.00	17	81
April	12.7	54.9	3.01	14.00	30	140
May	18.1	64.6	4.78	22.40	48	225
June	23.3	73.9	7.50	34.95	75	350
July	25.6	78.2	9.15	43.00	91	432
August	24.9	76.8	8.60	40.20	86	405
September	20.8	69.4	6.06	28.20	60	285
October	14.5	58.1	3.51	16.30	35	166
November	7.0	44.6	1.83	8.55	19	86
December	0.7	33.3	1.06	4.96	11	50
Total	151.8	657.5	49.04	229.13	491	2,306
Average	12.7	54.8	4.09	19.1	41	192
Equivalent temperature			16.3	61.3	16.3	61.3

reports on a Fahrenheit basis, equation 2 may be rewritten as

$$A=K_1\epsilon^{0.0481T} \qquad (3)$$

where T = temperature in degrees Fahrenheit.

For an example of the use of these formulas, only monthly mean temperatures will be used. Account will be taken of range of daily mean temperatures later. The data for Kansas City, Mo., are given on attached Table I. Column 2 gives monthly mean temperatures. Column 4 gives aging units for these temperatures obtained by substituting the temperature values in equation 2. Since the aging data are to be reconverted to temperature data, the constant K_1 may be taken as unity. The average of column 4 reconverted to temperature by equation 2 gives the equivalent annual temperature, that is, the temperature giving an average monthly aging that would result in the same total aging for the year as for the temperature cycle of column 2. Calculations with equation 3 are shown in columns 3 and 5 of attached Table I.

A graphical solution of the same data may be obtained by plotting equations 2 and 3 as shown in Figures 1 and 2, respectively. Figure 1 is plotted for the eight-degree-centigrade rule by doubling the aging unit abscissa for each eight-degree-centigrade increase in the temperature ordinate. The curve of Figure 2 is plotted in the same manner for 14.4 degrees Fahrenheit. As in the mathematical solution, the value chosen for the constant is unimportant.

Columns 6 and 7 show the use of these curves in obtaining the equivalent ambient temperature graphically. It will be noted that the use of varying constants gives different aging units, but that the temperatures check.

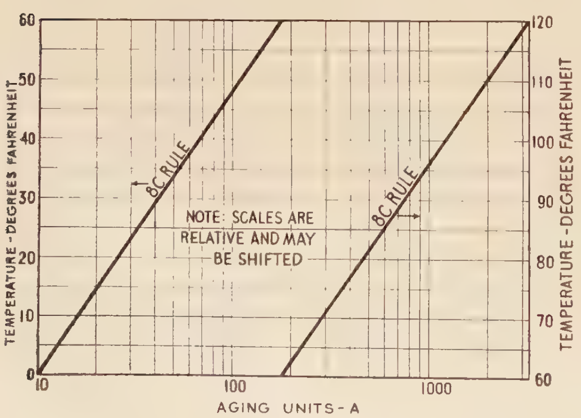

Figure 2. Effect of temperature on aging of transformer insulation

Calculation of Equivalent Ambient Temperature—Approximate Method

A survey was made of climatological data issued by the United States Weather Bureau for 23 cities in the United States (see Table II). Equivalent annual ambient temperature was calculated by the direct graphical method described above from normal monthly mean temperatures. These data were plotted as divergence K_8 from the average annual temperature as a function of range R during the year of monthly mean temperatures. The results are shown as the K_8 curve on Figure 3.

The effect of range during the month of daily mean temperatures was not calculated nationally because of the prodigious quantity of work required. However, a complete check was made for Chicago and had already been made for six other cities in a recent paper by Hellmund and McAuley.[8] It was found that the true equivalent ambient temperature could be approximated by using the curve K_8 of Figure 3 and adding ten de-

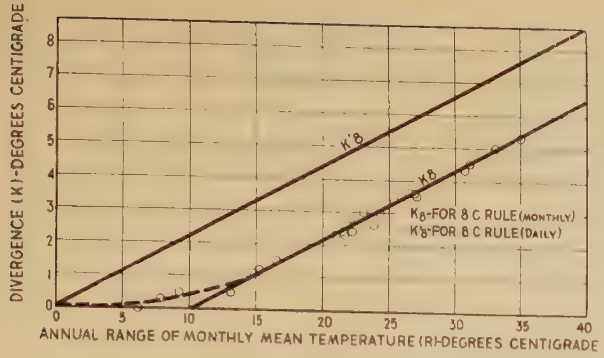

Figure 3. Divergence K between equivalent annual ambient temperature T_e and annual average ambient temperature T_a as a function of annual range R of monthly mean temperature

grees centigrade to the range over the year of monthly mean temperatures. The curve K'_8 is the K_8 curve moved up ten degrees centigrade so that divergence K'_8 between equivalent annual ambient temperature and average annual ambient temperature may be read directly as a function of annual range R.

This adjustment of ten degrees centigrade is no exact value taken from climatological summaries. Reference to Figure 4, in which the data for the Chicago five-year summary are given in curve form, will show the daily mean temperature curve to be very close to the hourly temperature curve. This is because the recorded daily mean is usually higher than the weighted mean, especially in the summer. The difference between the curve of daily mean temperatures and that of monthly mean temperatures is a matter of only a few degrees except at the end points. Any adjustment must take care of both the average difference of a few degrees and this end-point difference. The ten-degree-centigrade adjustment was used because it closely approximates calculated data, and because it gives a curve that is easily and conveniently used with the readily available monthly mean data. Thus, if

1. July average temperature = 25 degrees centigrade

2. January average temperature = −5 degrees centigrade

3. Annual average temperature = 10 degrees centigrade

then annual range

R = 30 degrees centigrade
K'_8 = 6.5 degrees centigrade
T_e = 16.5 degrees centigrade

For six cities[8] the equivalent ambient temperature had already been calculated by the direct method from summaries of hourly temperatures for the six-year period of 1932–37, inclusive. Similar calculations were made for Chicago for the period of 1937–41, inclusive. The results were then compared with the approximate values of equivalent temperature from Table II, column 8. A preliminary check-up had shown a difference between normal annual mean temperature and mean temperatures for the period covered by the summaries. It was necessary to adjust the direct calculations for this difference. The results are shown in Table III. It can be seen that the correlation between results by the two methods is within the accuracy of the problem as a whole.

The methods described above for obtaining equivalent annual ambient for air-cooled transformers may be applied to water temperature for water-cooled transformers. This is especially applicable when the cooling water is obtained from a lake or river so that there is a considerable annual range in water temperature. For Lake Michigan water used in Chicago, the averages of the monthly maximum temperatures over a 15-year period were used to give the annual temperature cycle. The equivalent temperature obtained by using Figure 1 checked very closely with that obtained from use of K_8 in Figure 3.

Application to Transformer Loading

For constant load over the year or for an annual load cycle that has its maximum in summer, the maximum permissible load for normal life can be based on the equivalent annual temperature of the cooling medium obtained by the methods herein described. This is applicable both to water-cooled and to air-cooled transformers. The increase in loading for reduction in ambient temperature for

Table II. Equivalent Annual Ambient Temperature

	Based on Monthly Mean Temperatures					Based on Daily Mean Temperatures Using K'_8 of Figure 3	
City	Annual Average Temperature (T_a) (2)	Annual Range (R) (3)	Equivalent Annual Ambient Calculated (T_e) (4)	Divergence (K_8) Calculated (5)	Divergence (K_8) From Figure 3 (6)	(K'_8) (7)	(T_e) (8)
Bismarck	4.7	35.0	10.0	5.3	5.4	7.6	12.3
St. Paul	6.8	33.1	11.8	5.0	5.0	7.1	13.9
Duluth	3.3	31.1	7.8	4.5	4.5	6.7	10.0
Des Moines	9.7	30.7	14.0	4.3	4.5	6.6	16.3
Chicago	9.5	27.1	13.0	3.5	3.7	5.8	15.3
Kansas City	12.7	26.9	16.3	3.6	3.7	5.8	18.5
St. Louis	13.3	26.5	16.9	3.6	3.6	5.7	19.0
Helena	6.2	25.3	9.6	3.0	3.3	5.5	11.7
Boston	9.8	24.3	12.8	3.0	3.1	5.2	15.0
New York	11.3	23.8	13.9	2.6	2.9	5.1	16.4
Denver	10.0	23.0	13.0	3.0	2.8	5.0	15.0
Santa Fe	9.3	22.2	11.7	2.4	2.7	4.8	14.1
Memphis	16.4	22.1	19.2	2.8	2.7	4.8	21.2
Dallas	18.4	21.6	20.7	2.3	2.5	4.7	23.1
Phoenix	21.1	21.5	23.5	2.4	2.5	4.6	25.7
Raleigh	15.6	20.9	17.9	2.3	2.3	4.5	20.1
Galveston	20.9	16.5	22.3	1.4	1.4	3.5	24.4
Portland, Oreg.	11.7	15.2	12.9	1.2	1.2	3.3	15.0
Jacksonville	20.7	14.8	21.8	1.1	1.1	3.2	23.9
Seattle	10.6	13.1	11.1	0.5	1.0	2.8	13.4
Los Angeles	16.8	9.2	17.3	0.5	1.0	2.0	18.8
Key West	24.9	7.8	25.2	0.3	1.0	1.7	26.6
San Francisco	13.4	6.1	13.4	0	1.0	1.3	14.7
Average	12.9	21.6	15.5	2.6	2.7	4.7	17.6
National mean	12.3	22.8	14.9	2.6	2.8	4.9	17.2

All temperatures are in degrees centigrade.

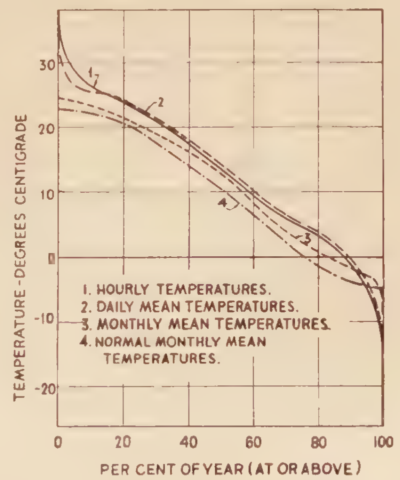

Figure 4. Outdoor ambient temperatures, Chicago, Ill. (From U. S. Weather Bureau data, 1937–41, inclusive)

Table III. Equivalent Ambient Temperature—Comparison of Direct and Approximate Methods

City	Annual Average Temperature	Equivalent Annual Temperature: Direct Method, Calculated[8]	Direct Method, Corrected to Normal*	Approximate Method from Figure 3
Bismarck	4.7	14.7	13.6	12.3
Helena	6.2	12.9	12.1	11.7
Kansas City	12.7	20.1	19.2	18.5
Denver	10.0	16.2	15.0	15.1
Dallas	18.4	22.0	21.6	23.1
Santa Fé	9.3	14.0	13.4	14.1
Chicago	9.5	15.2	14.3	15.3
Average	10.1	16.4	15.6	15.7

All temperatures are in degrees centigrade.

* Calculations are based on six- and five-year summaries. Correction is made for difference in mean temperature between summary period and normal.

self-cooled transformers according to American Standard *C*-57 is one per cent for each degree that the temperature is below 30 degrees centigrade. For the temperature data given above, the equivalent annual ambient was 16.5 degrees centigrade, or 13.5 degrees below the standard ambient temperature. Therefore, the permissible constant load throughout the year would be 113.5 per cent of rated load, with normal life expectancy. However, if the average monthly temperature were used as the basis of increased loading, the July average ambient temperature of 25 degrees centigrade would limit the permissible overload to 105 per cent.

For other types of loading, the procedures herein described would not be directly applicable without adaptation to each specific loading condition.[9] For example, for a constant load occurring only a few hours each day, it would be feasible to consider the mean ambient temperatures for those hours rather than for the entire day. When winter and summer loads are not the same, it would be feasible to calculate an annual aging curve based on a combined load and temperature cycle in order to fix the maximum level of the annual load curve as a whole.

Conclusion

The approximate method herein described may be used to obtain equivalent annual temperature from readily available climatological data. For loading on an annual basis the maximum permissible load for normal life can be based on the equivalent ambient temperature.

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Interim Report on Application and Operation of Circuit Breakers and Switchgear

AIEE COMMITTEE ON PROTECTIVE DEVICES
Subcommittee on Circuit Breakers, Switches, and Fuses

THE present war emergency makes it necessary that maximum use be made of existing equipment and that a minimum amount of critical materials be used for new equipment. The following guides have been prepared as an aid to those involved in the application and operation of circuit breakers and switchgear.

This report is intended to cover equipment of the following types:

1. A-c power circuit breakers above 600 volts, both indoor and outdoor.
2. Air disconnecting switches.
3. Switchgear assemblies.

Basis of Design of Apparatus

The name-plate rating of apparatus does not necessarily indicate the load which can be carried safely under all conditions. It specifies the load which may be carried without exceeding the specified temperature rise. However, it is the total temperature and the duration of such temperature which determine the life of apparatus. Obviously the total temperature depends not only on the loading but on the ambient temperature as well.

The present standards for circuit breakers and switchgear provide that the temperature rise of the contacts shall not exceed 30 degrees centigrade at rated load and that the apparatus shall be suitable for continuous operation at rated load, provided the ambient temperature does not exceed 40 degrees centigrade for apparatus with plain copper contacts or 55 degrees centigrade if the contacts and other connections are silver or equivalent. This means that the total temperatures should not exceed 70 degrees centigrade for plain copper or 85 degrees centigrade for silver contacts.

It should be noted that the temperature rise of contacts may increase as a result of oxidation of the contact surface, and, therefore, the standards are based on sufficient maintenance to keep the temperature rise within specified limits.

Effect of Ambient Temperature

It is apparent that, if the ambient temperature is higher than these values, the loading must be less than the name-plate rating to avoid exceeding the permissible total temperature. It is also apparent that if the ambient temperature is lower than these values, the loading may be greater than the name-plate rating. The loading should be reduced approximately two per cent for each degree that the ambient temperature is above these values, and in some cases it may be increased as much as one per cent for each degree that the ambient temperature is below the values specified in the standards. See Figure 1 for relation between loading and ambient temperature for apparatus with silver contacts. These factors should not be used to increase the loading more than 30 per cent without special consideration.

The foregoing recommendations may be applied for average conditions, but they do not represent necessarily the maximum in all cases. This is due to the variations in thermal characteristics of different designs and sizes of switching equipment and variations in types of installation. Therefore, to determine the maximum carrying capacity of individual installations, studies or tests should be made to establish the proper relation between loading and ambient temperature. This is particularly true in the case of heavy current installations.

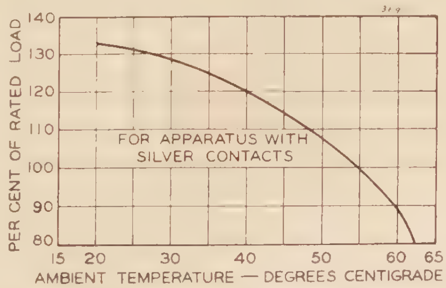

Figure 1. Relation between loading and ambient temperature

Artificial Cooling

Artificial cooling may be used to increase the permissible loading in some installations. This may take the form of forced ventilation of the room in which the apparatus is located, or it may involve forced ventilation through the equipment itself. For oil-filled equipment there may be some possibility of artificial circulation of the oil to assist in the transfer of heat and thus increase the permissible loading. Extreme care must be used in any application of forced cooling to be sure that there are no parts of the equipment which do not benefit by the forced cooling and might reach excessive temperatures. It should be recognized that forced ventilation may tend to increase the danger of fire spreading to other parts of the installation.

Emergency Loading

Under emergency conditions the permissible loading may be higher than under normal conditions, particularly if the duration is not too long and if the condition does not recur frequently. It is impracticable to assign specific values for all types of switchgear. The characteristics of the specific apparatus involved should be thoroughly investigated in connection with the determination of limitations of emergency loading. Also, the heating effect of external connections should be considered. Operation at higher temperatures may require additional maintenance.

Modification of Existing Equipment

The permissible loading of existing circuit breakers and switching equipment may be increased in many cases, particularly the older types of equipment with plain copper contacts, by silver plating the contacts and connections. Means are available for silver plating with a special form of brush. In many cases this can be done without removing the parts or even disturbing the adjustment of circuit breakers and switches. If the loading is increased by resorting to such

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Personnel of subcommittee on circuit breakers, switches, and fuses: R. T. Henry, *chairman;* A. E. Anderson, H. D. Braley, O. E. Charlton, F. W. Cramer, W. Deans, W. S. Edsall, F. R. Ford, H. W. Haberl, H. J. Lingal, L. R. Ludwig, J. B. MacNeill, H. H. Marsh, Jr., J. R. North, H. V. Nye, M. S. Oldacre, H. H. Rudd, R. M. Smith, H. P. St. Clair, H. E. Strang, H. R. Summerhayes, J. D. Wood.

treatment, care must be used to insure against excessive temperatures in other parts of the apparatus. Various papers[6,7,8] indicate some of the possibilities along this line.

The interrupting capacity of existing circuit breakers can be increased in many cases by the installation of new contact systems involving oil blast, deion grids, ruptors, and so forth, together with other parts as necessary, depending upon the type of breaker and its manufacturer.

Current transformers should be carefully considered to determine their actual carrying capacity. Many current transformers are capable of carrying more than their rated load continuously, and most of them can carry short-time emergency loads higher than their rating. "The Interim Report on Transformers"[5] gives useful information in this connection. Studies or tests should be made to determine the maximum carrying capacity of individual installations.

Fuses generally do not have excess current-carrying capacity corresponding to most other devices in that they are fundamentally a thermal overload protective device and accordingly cannot be given overload ratings. In the application of fuses where selective operation is involved, consideration should be given to the effect of ambient temperature on the characteristics of the fuses.

Simplification of New Installations

In selecting equipment for new installations every effort should be made to use the minimum amount of critical materials. Probably the greatest saving can be made by choosing a simple scheme of connections using the minimum amount of equipment consistent with the requirements. Duplication of busses and switching equipment may be avoided in many cases, at least for the duration of the present emergency. The margin between the rating of apparatus and the actual load may be less than might be considered satisfactory under normal conditions.

Where additions are to be made to existing installations of two-bus design, it may be feasible to connect the new circuits to only one of the busses, at least during the emergency. Equipment for such additions may even be obtained by removing some of the existing connections to one of the busses.

The short-circuit current may be kept low in many cases by sectionalizing or other means to permit the use of smaller circuit breakers or existing circuit breakers which might not be satisfactory under normal conditions after the emergency is over.

A large circuit breaker of adequate interrupting capacity may be used in some cases as a backup for a group of smaller circuit breakers, with the protective relay system designed to permit automatic operation of the individual small breakers on faults within their interrupting capacity but locking out and transferring the operation to the large circuit breaker on faults exceeding the interrupting capacity of the small circuit breakers. In such cases the momentary rating of the small circuit breakers should be carefully considered.

In some cases it may be possible to open one or more of the supply circuits in order to reduce the interrupting duty to a value which the otherwise inadequate circuit breakers are capable of interrupting.

In transformer substations with a single transformer bank it may be permissible to omit the circuit breaker on either the high-voltage or the low-voltage side.

In laying out new switching structures the cross section of the busses may be reduced for part of their length by grouping the heavier circuits close to each other and placing the lighter circuits toward the ends of the bus in some cases.

It may be possible in other cases to locate the main incoming circuit near the center of the bus to permit tapering the bus toward the ends.

In outdoor structures it may be possible to use galvanized iron pipe for conductors instead of copper tubing or to substitute other forms of steel for copper conductors in many cases.

In the cases of busses designed to carry heavy current, appreciable amounts of material may be saved by proper consideration of the configuration of the material used. When flat bars are used, the heat dissipating qualities will be considerably improved if the bars are placed on edge rather than flat in order to improve the ventilation. When multiple flat bars are used, consideration should be given to the proper spacing to improve the division of current between the individual bars. In some cases tubular or hollow square sections or assemblies built up of two angles to form a square provide efficient use of the materials. In some cases the carrying capacity of bus bars may be increased approximately 10 per cent by painting the bars with a dull black paint which is more favorable to the emission of heat than a bright surface. Various papers and publications[1,2,3,4] cover the design of busses for heavy current.

Tinning or silver plating all contacts will permit satisfactory operation at higher temperatures than would be the case with plain copper contacts.

Where multiple cables are employed in high-capacity circuits, careful attention should be given to the physical arrangement in order to divide the current evenly between the various cables. It should be noted that the configuration of multiple cables in a flat spacing may result in the outer cables carrying more than their share and the inner ones carrying less than their share of the total current. In some cases this condition can be greatly improved by the installation of small "doughnut type" reactors around the cables which tend to carry the most current.

Wood structures may be used instead of steel. These may be permanent structures in some cases and even greater savings may sometimes be made by using temporary wood structures with a bare minimum of equipment which might not be considered satisfactory for normal conditions after the present emergency is over. Obviously simple temporary structures should be used where there is a reasonable possibility that the use may be limited to the duration of the emergency.

In the interior wiring of new plants the more general use of higher distribution voltages may result in appreciable savings of critical materials. Consideration should be given to using 600 volts instead of 480, and 480 volts instead of 240.

Reactive current is a very important consideration both in industrial plants and in utility systems. Appreciable savings may be made in many cases if the reactive current is supplied at the load rather than from the utility system. Supplying the reactive current at the load results in less voltage drop, less energy loss, and releases system capacity which may be needed to carry other useful load. In many cases the reactive current can be supplied at the load by using synchronous motors, synchronous condensers, or capacitors.

Capacitors may be used to make appreciable savings of critical materials, particularly if they are scattered and connected as near as possible to the various loads. In fact, capacitors connected directly to the leads of each motor should be considered, at least for all but the very small motors. Considerable savings may often be made in the plant wiring in this way. It may be desirable or necessary in some cases to make provision for disconnecting the capacitors during light load periods.

New Developments in Potential-Transformer Design

G. CAMILLI
MEMBER AIEE

Synopsis: This paper describes certain design ideas applied to potential transformers to accomplish substantial reductions in volume and weight, with improved reliability and with better accuracy than the best requirements of the American Standards. The size of these units is such that they appear mostly as bushings.

Perhaps the most striking feature of construction is the arrangement and the insulation of the high-voltage winding. Liquid-impregnated porous paper has been substituted for the combination liquid and solid used in the conventional designs. Another notable improvement incorporated in these transformers is the elimination of all gaskets. The amount of liquid insulation required is extremely small, and therefore premium insulating liquids (such as the Askarels) can be used without much additional expense.

IN the past, the design of liquid-filled potential transformers, perhaps with the exception of the cascade connected units[1] has followed very closely that of power transformers. Thus, until very recently, a 69-kv potential transformer was designed and built essentially according to the same principles and practices as followed in the design of a 69-kv small distribution transformer, with the result that the units were bulky, and their size and weight were out of proportion with their operating duties. A comparison of the new with the old is given in Table I in which it will be seen that the new units are a little over 40 per cent of the weight of the older ones.

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G. CAMILLI is development engineer with General Electric Company, Pittsfield, Mass.

The author acknowledges the assistance of several of his associates, particularly J. J. Vienneau.

Principles of Design

High-Voltage Winding

The high-voltage winding of the new potential transformers is the concentric layer-wound type, shown in Figure 1 for a transformer for connection between line and ground.

This type of construction consists of a plurality of layers of winding and shields so disposed as to give uniform voltage distribution. The innermost layer adjacent to the low-voltage winding starts from the neutral and the successive layers are connected in series progressively, the outermost layer being connected to the line. This winding is placed between two cylindrical shields; one is connected to the innermost layer and eventually to ground, while the other shield surrounds the last (outermost) layer and is connected to the line.

With this arrangement properly proportioned, the relative position of each part of the winding in the electrostatic field between the two shields will not deviate greatly from its actual low-frequency position. Thus, when an impulse voltage is imposed on the line end, it will be substantially uniformly distributed throughout the winding, regardless of the steepness of the oscillation of the applied wave. It is obvious, therefore, that the insulation between the shields and the adjacent layer and between successive layers is only that required to withstand the voltage developed within the individual layers.

Scheme of Design for Cascade Connection

One practical scheme of reducing the size and weight of those transformers which are normally used for operation between line and neutral of a solidly grounded system is by means of the so-called cascade connected units.[1] In this design the insulation can be distributed to the best advantage.

Figure 2 shows the multilayer type of winding construction as is ordinarily used in a cascade potential transformer.

The deciding factors between the single-stage and cascade potential transformers are the economy and accuracy.

In general, the accuracy of the cascade potential transformer is not as great as that of the single-stage type. For voltages higher than 115 kv, it appears that the cascade type is somewhat more economical than the single-stage.

Scheme of Design for Isolated Operation

Figures 3 and 4 show the multilayer type of construction which could be used for a fully insulated transformer for connection from line to line of an isolated system.

In Figure 3 only one high-voltage coil section is used. This type of construction may be applied only to relatively low voltages because of difficulty which is

Conclusions and Cautions

From the foregoing it is apparent that the loading on existing equipment may be increased in many cases and that new equipment may be selected with a view to using a minimum of critical material during the present war emergency. Other suggestions and more details may be found in the papers and publications listed at the end of this report.

It must be recognized that the loading on equipment should not be increased without a thorough study of the limitations of the apparatus involved and of associated equipment such as cables, terminals, current transformers, soldered connections, and so forth. It must also be recognized that increasing the loading on equipment is almost certain to require more maintenance and may shorten the life of some apparatus, but it is undoubtedly true that appreciable savings of critical materials can be made without sacrificing life unduly and without requiring excessive maintenance.

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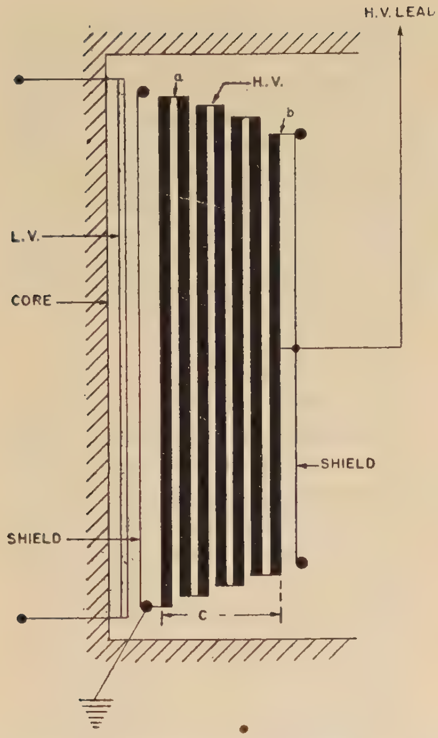

Figure 1. Transformer for operation between line and grounded neutral

encountered in bringing out the high-voltage terminal at the beginning of the high-voltage winding.

For this reason, the scheme shown in Figure 4 is preferred. In this case, the high-voltage winding is divided in two equal sections which are connected together at starting ends of the windings. Each section is of the concentric multilayer type similar in all respects to the arrangement shown in Figure 3, except that the spacing between the inner shield and the low-voltage winding is, of course, designed to withstand a test voltage equivalent to the full line-to-line voltage.

Novel Method of Insulating the High-Voltage Winding

The new method of insulating the high-voltage winding is shown, schematically, in Figure 6. To make the matter more clear, let us compare the new with the old. In potential transformers of the conventional type, the distance between the high-voltage and low-voltage winding of, say, a 138-kv line-to-line connected potential transformer has been essentially the same as the distance between the high-voltage and low-voltage windings of a power transformer of the same voltage rating. Furthermore, in a power transformer this insulating distance in general consists of one or more liquid ducts interleaved with solid insulating barriers. This same type of insulation (liquid ducts plus solid barriers) is used between the high voltage and the core. In a power transformer the liquid ducts have a useful function, the cooling of windings. Such duty is not required in a measuring transformer, because the losses, under ordinary conditions, are negligibly small. A simple calculation of I^2R loss in the high-voltage, or primary, winding of a 138-kv potential transformer may be of interest to those not familiar with the problem. Let us assume that the resistance of such winding is 10,000 ohms. The copper loss at 500 volt-amperes (which is the normal rating of a potential transformer) is then

$$\left[\frac{\text{Output volt-amperes}}{\text{Primary winding volts}}\right]^2 R = 0.13 \text{ watt}$$

which is a very negligible quantity. It is apparent then that the cooling of the high-voltage winding is of secondary importance, and it is, therefore, not necessary to have any liquid ducts. It is well recognized that, in an insulating structure comprising oil operating in series with solid insulation, the dielectric stress is inversely proportional to their dielectric constants, and ordinarily the solid insulation is not effectively utilized.

In the new design, the complete insulating structure consists of solid insulation which is impregnated with an insulating liquid. This combination may be considered a uniform insulating mass having a dielectric constant intermediate between that of the liquid and solid insulation, and its dielectric strength approaches the impregnated paper of an oil-filled cable.

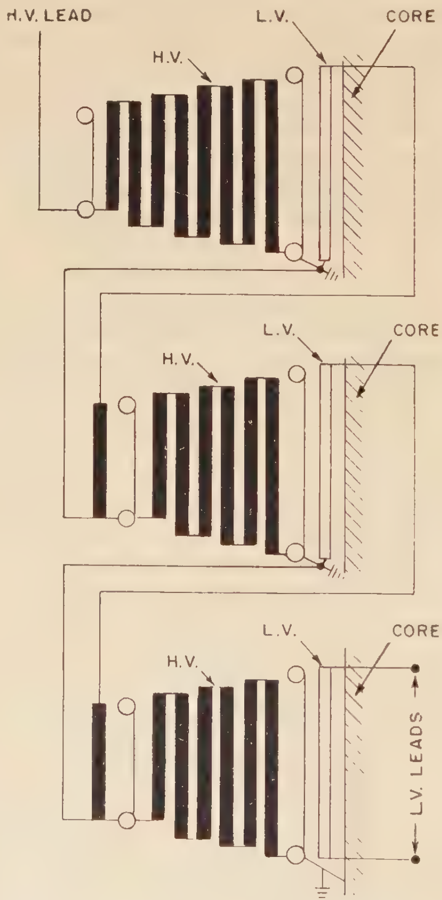

Figure 2. Transformer for operation between line and grounded neutral (cascade type)

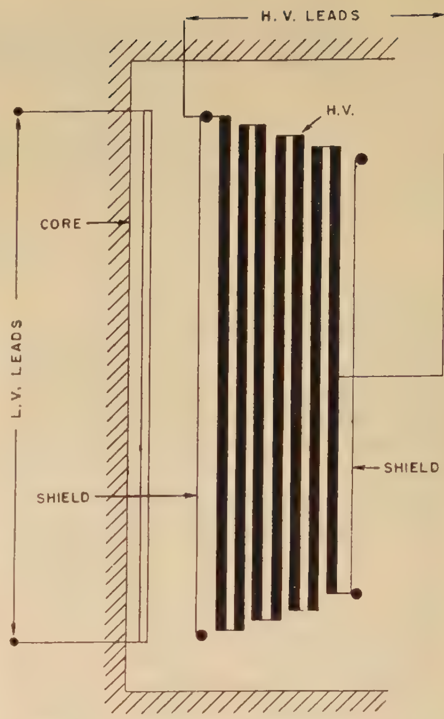

Figure 3. Transformer for operation between lines

Referring now to Figures 4 and 5, the insulation between layers is easily provided for. This may consist of several sheets of paper having folded edges with the layers located between the folds. Such a construction leads to a coil having a small radial thickness. The shield and successive layers give a very good distribution of voltage stress so that the insulation between layers is well proportioned to the stress. This type of construction would be very desirable if the dielectric stresses at the ends were not distributed between the liquid and solid insulation. As has been discussed previously, this condition gives rise to inefficient use of insulation.

Referring to Figure 5, one method of insulating the ends of the layer of the coil would be by the use of very wide folds in the layer insulation and by the use of stepped pressboard barriers as shown. Such an arrangement, however, is not very efficient since the path A from the shield to ground is, in effect, outside the uniform voltage distribution dictated by the high-voltage winding and therefore to avoid creepage failures must be made unnecessarily long.

The good advantages of the cylindrical layer-wound coil, therefore, would be lost unless some more efficient method is found to insulate the ends of the winding. The

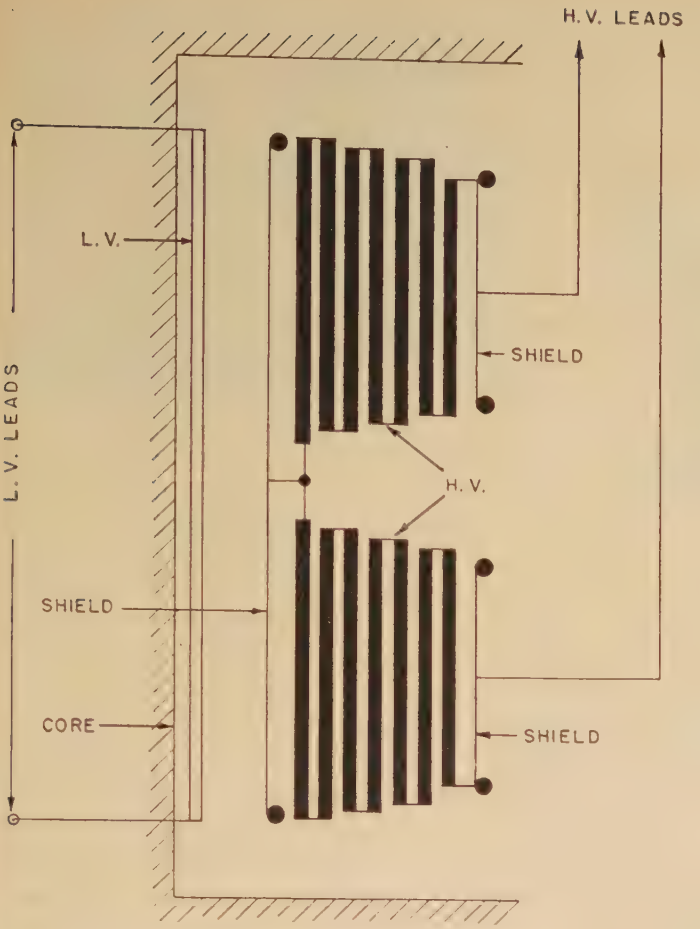

Figure 4. Transformer for operation between lines

scheme shown in Figure 6 seems to offer a good solution to this problem. In this method, the layer insulation consists of paper with folded edges and additional layers of paper. The width of the latter is much longer than the width of the winding layer. After the coil is wound, the extensions of the paper are folded over the shield as shown in the illustration. It will be noted that such a method of insulation not only solves the problem of insulating the ends of the coil, but automatically provides the necessary solid insulation between the line shield and the core (ground) (path B of Figure 5).

In using the method of insulating shown in Figure 6, the coil design is so arranged that the insulation at the ends of the winding cuts the electrostatic line of force at right angles and at approximately equipotential steps, thereby realizing all the benefits of cascading the voltage. The application of this type of insulation to transformers with both bushings isolated is shown in Figure 7. The high-voltage coil is divided in two sections A and B. Each coil is insulated in the same manner as illustrated in Figure 6; pressboard barriers are placed between the two sections, and the entire coil assembly is covered with layers of paper. Thus, oil ducts of previous designs are completely replaced by solid insulating material, and, in a manner similar to the current transformers described in another paper,[2] the entire electrostatic field contains liquid-impregnated porous paper of uniform permittivity.

Such a method of insulation with closely wound and insulated coils has, of course, required the development of a new treatment and impregnating process.

By carefully controlled materials and methods, a very flat power-factor temperature curve is obtained, and any danger of thermal instability is eliminated.

Impulse Characteristics of the High-Voltage Coil Shown in Figure 7

Experimental data and theoretical study (see Appendix) have shown that when this type of high-voltage winding is subjected to impulse voltages with vertical front waves under the worst conditions, approximately 75 per cent of the total voltage appears across one section. Contrary to the behavior of the winding of conventional design, this voltage is approximately uniformly distributed among successive winding layers, and therefore at no point within the high-voltage winding is it necessary to provide insulation to withstand more than the proper share of the voltage appearing across half the winding.

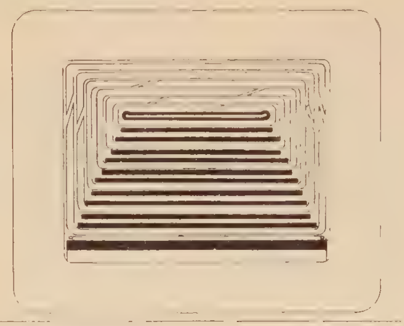

Figure 6. Improved method of insulating the high-voltage winding (for grounded neutral transformer)

Magnetic Circuit

Some of the benefits of such an efficient coil construction would be lost without a companion magnetic circuit of equal efficiency. The wound-core type of construction,[3] so successfully used in distribution transformers, has been adopted in the new type of potential transformers. The magnetic circuit is formed from magnetic strip material, spirally wound flatwise. The core closely embraces the

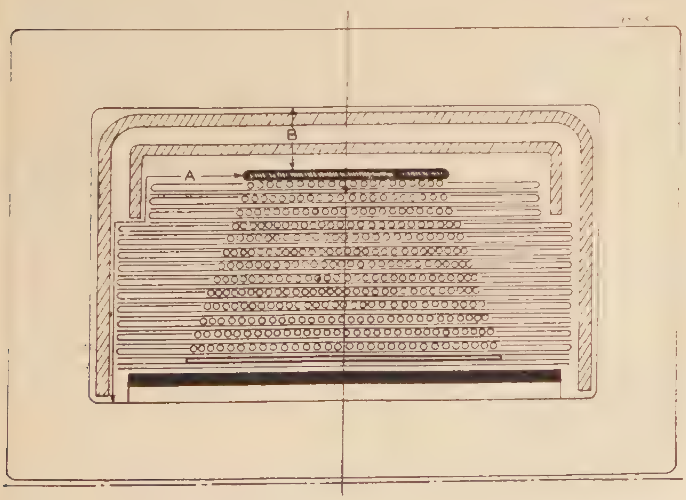

Figure 5. Method of insulating the high-voltage winding (for grounded neutral transformer)

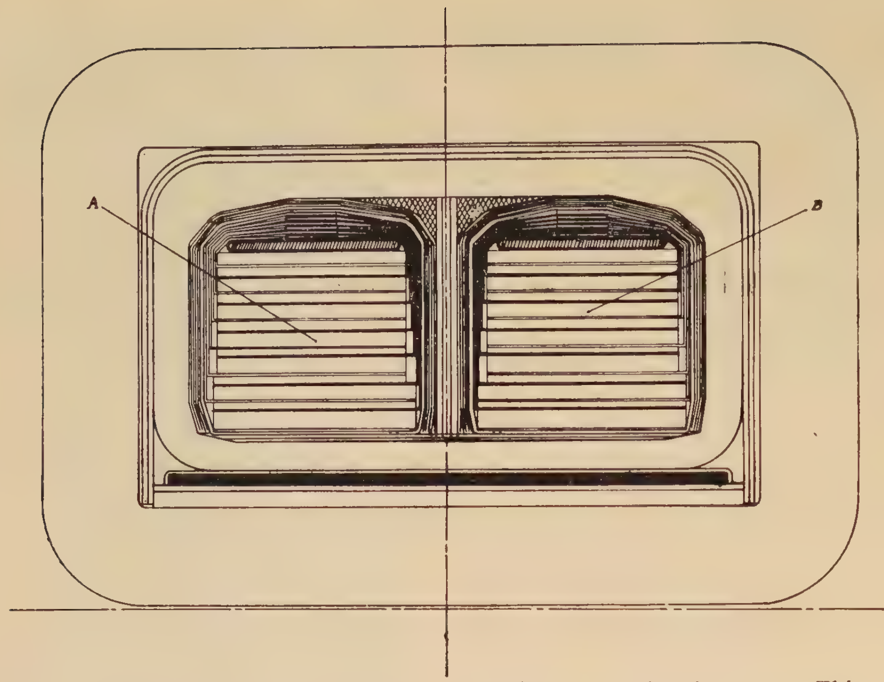

Figure 7. Improved method of insulating the high-voltage winding (transformer for operation between lines)

high- and low-voltage coils so that the coil structure substantially fills the window of the core, providing a high space factor which reduces its volume and weight.

Bushing Design

In the conventional type of high-voltage potential transformers (Figure 8a) which are provided with liquid-filled bushings, there is not connection between the liquid in the terminals and that in the tank. To provide for the expansion of the main liquid, it is necessary to leave an air space under the cover. This air chamber is responsible for the long ground sleeve of the bushings since the ends of these sleeves must always be under oil. Such transformers are necessarily tall and bulky.

If the joint between the tank and the cover is made liquid tight, it is possible to suppress the lower portion of the bushings. Evidently, the expansion chamber of Figure 8a, in the latter case, may be transferred to the top of the bushings. The size of the latter expansion chamber, of course, would be too large if applied to a transformer of the conventional type containing a large amount of liquid. In the new transformers, the amount of liquid is only a small fraction of that used in the conventional design, and therefore it has been found practical to enlarge the top of the porcelain and to use it for the expansion of the liquid. Such a construction is illustrated in Figure 9. In the new design the terminals are made an integral part of the high-voltage coils and are extended inside the porcelain shells.

Table I

	Relative (Per Cent) Values			
	34,500/115 Volts		69,000/115 Volts	
	Old	New	Old	New
Over-all height	100	51	100	51
Width of base	100	100	100	108
Net weight	100	41	100	42
Liquid volume	100	13	100	16

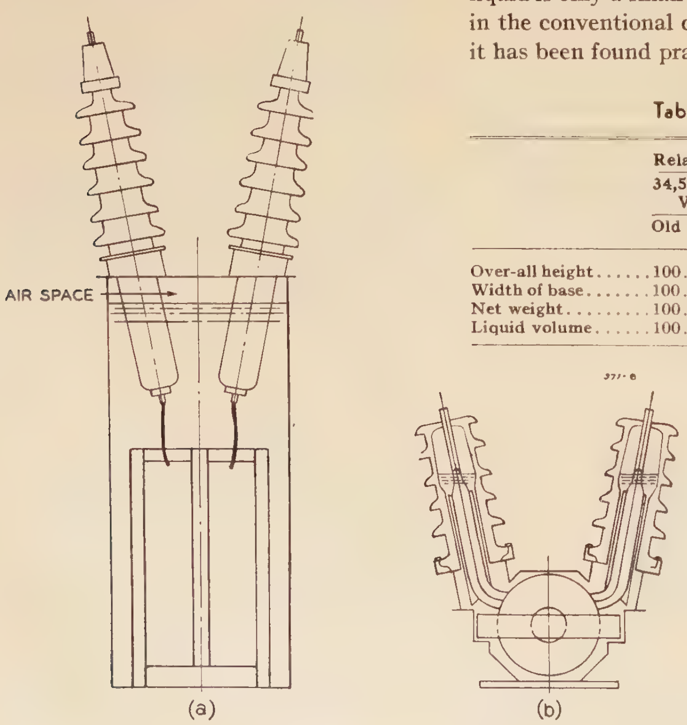

Figure 8. Comparison between old and newly developed potential transformers

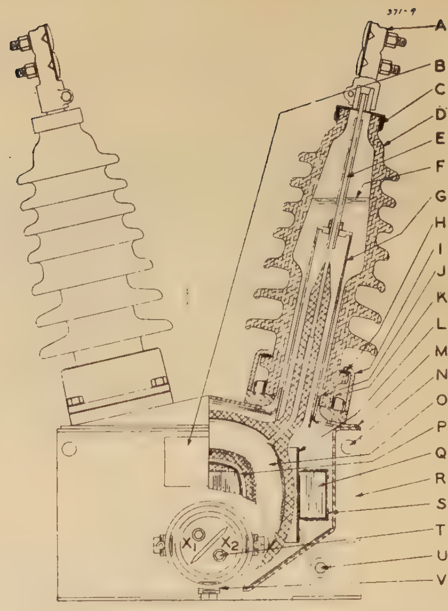

Figure 9. Cross section of the new potential transformer

(a). High - voltage clamp-type terminal
(b). Name plate
(c). Seal
(d). Porcelain bushing
(e). High - voltage lead
(f). Liquid level
(g). Insulating tube
(h). Cushion
(i). Mechanical clamping ring
(j). Insulating collar
(k). Seal
(l). Insulating shield
(m). Welded joint
(n). Lifting hole
(o). High-voltage coil
(p). Low - voltage coil
(q). Core
(r). Tank
(s). Core clamp
(t.) Low - voltage terminals
(u). Ground terminal
(v). Low - voltage outlet; remove pipe plug for one-inch conduit

Gasketless Construction

Another notable improvement incorporated in the new design potential transformers is the elimination of all gaskets. The cover and the tank are welded together, and the high-voltage porcelain bushings are sealed to the cover. Similarly, the low-voltage glass bushings[4] shown in Figure 10 are welded to the rank. The cross section of one of the high-

Table II

Voltage Rating (Kilovolts)	Maximum Rating (Kilovolt-Amperes)
24	2
34.5	2.5
46	3
69	3.5

Figure 10. View of low-voltage glass bushing

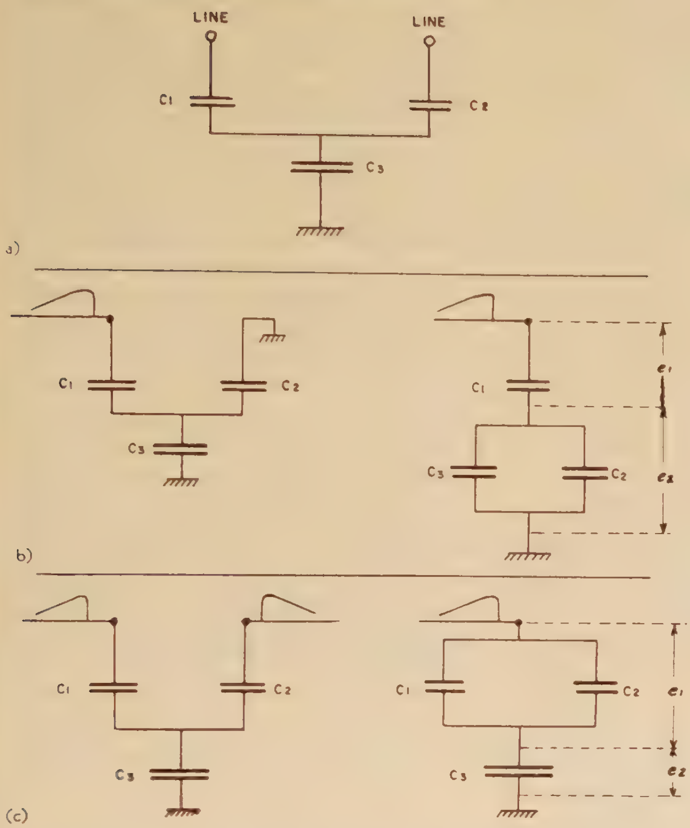

Figure 11. Equivalent circuit of transformer shown in Figure 4 subjected to impulse voltages

voltage bushings is shown in Figure 9. It should be noted that the functions of the mechanical clamping and the sealing of the bushing are independent from each other, thus relieving the seal of any mechanical stresses.

Use of Noninflammable Liquids

In view of the fact that the amount of insulating liquid required in this design is about one sixth to one eighth of the old (see Table I), these units can be insulated with noninflammable premium insulating liquids with very little extra expense.

Maximum Ratings of the New Transformers

The temperature rise of the new transformers is negligible when the units are operated at their normal rating of 500 volt-amperes secondary burden. The maximum rating, on the basis of 55 degrees centigrade with 30 degrees centigrade ambient is given in Table II.

Conclusion

Recent progress in potential transformer design has been primarily due to the development of a novel method of constructing and insulating the high-voltage winding. This novel winding also can be used advantageously with the wound-core construction and can be hermetically sealed without gaskets.

The new transformers are compact and light with accuracy equal to that of previous design. The compactness of these transformers permits the use of premium non-inflammable insulating liquids at little extra cost. The new transformers require little or no inspection and can be mounted in the bus structure near the high-voltage lines.

Appendix

The equivalent circuit of the coils shown in Figure 4, under Impulse Voltages, may be represented as shown in Figure 11a. There are two conditions to be examined:

(*a*). Impulse wave reaching one line while the other end is grounded.

(*b*). Impulse wave reaching simultaneously from both lines.

1. Impulse incoming from one line with the other end grounded. Referring to Figure 11b we have:

$$\frac{e_1}{e_2}=\frac{C_2+C_3}{C_1}$$

$$\frac{e_1}{e_1+e_2}=\frac{C_1+C_3}{2C_1+C_3}=p1=\text{fraction of the total}$$

voltage appearing across C_1 (1)

If we assume (as is usually the case) that $C_3=2C_1$ then from equation 1 $p1=0.75$

2. Impulse incoming simultaneously from both lines. Referring to Figure 11c we have:

$$\frac{C_1+C_2}{C_3}=\frac{e_2}{e_1} \qquad \frac{C_3}{2C_1+C_3}=\frac{e_1}{e_1+e_2}=p1 \qquad (2)$$

If we assume $C_3=2C_1$, then $p1=0.5$

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A New Control System for Automatic Parallel Operation of Load-Ratio-Control Transformers

S. MINNECI
ASSOCIATE AIEE

S. B. FARNHAM
MEMBER AIEE

Synopsis: The successful parallel operation of transformers equipped with automatic load-ratio control and line-drop compensation requires that stability of operation be insured. In addition, experience has shown that it is frequently highly desirable that other automatic features be included which will contribute toward operating flexibility, and which will permit switching transformers in and out of parallel operation without disturbing the voltage level at the load center or causing an excessive initial circulating current at the instant of paralleling.

This paper discusses the conditions which make these added features desirable, and presents a new control system whereby they all may readily be secured. In this new system, load current and circulating current are made to flow in separate paths in the control circuit, and each produce their effects independently, thereby insuring stable operation and maintaining the desired load-center voltage level. This separation of the load and circulating current, in conjunction with a modification in the closing circuit of the paralleling circuit breaker, also makes possible the avoidance of excessive initial circulating current when the breaker is closed. The simplicity and effectiveness of this arrangement in providing a fully automatic means for meeting all the anticipated operating requirements recommends its use in many applications.

The economic trend in distribution-system arrangement toward locating individual substations at or near the center of each load area, plus increased attention to improved voltage regulation results in a growing number of relatively small substations.[1,2,3] These are frequently from 1,000 to 5,000 kva in size and are provided with means for automatically regulating the bus voltage. They are increasingly of the unit-substation type, in which the functions of three-phase voltage transformation, automatic voltage regulation, switching, metering, and protection are included in a co-ordinated and factory-assembled unit.[4,5,6] In such substations the voltage regulation is normally provided by transformers having built-in load-ratio-control mechanisms, capable of changing taps under load, and equipped with line-drop compensators for maintaining the desired voltage at the load center. Their load-ratio-control equipments are frequently called upon to operate in parallel with one another and with those of other substations having similar voltage-regulating equipment.

Parallel operation may be required where the load-ratio-control transformers are located some distance apart, and are joined together by interconnecting circuits of sufficient length to introduce a substantial amount of impedance between them; or they may be located immediately adjacent to one another and connected by circuits of very short length and low impedance. The former condition is typical of utility network distribution systems. The problems involved in securing successful parallel operation of the automatic voltage-regulating equipments in such network systems are well understood, and adequate means are available for solving them.[7]

A generally applicable and fully automatic control scheme for providing all of the features that are desirable in arrangements of the second type—wherein the units are physically and electrically close together—has, however, not heretofore been available. Typical of this class of application are those cases where rapidly growing local loads have necessitated the paralleling of load-ratio-control transformers in the same substation, or where two or more unit substations are installed side by side and interconnected by circuits of only a few feet in length. A similar condition exists in the case of all double-ended unit substations, in which two load-ratio-control transformers are made integral with a common switchgear section, and thereby connect directly to the same load bus.

The new control system presented here is particularly adapted to applications of this type, but the advantages which it offers may also be realized where the substations or load-ratio-control transformers are some distance apart, provided only that the distance is not beyond the feasible length of interconnecting control circuits. It permits the paralleling of any desired number of units, and yet retains full freedom for independent, nonparallel operation when required.

Operating Requirements

When two or more automatic load-ratio-control transformers with line-drop compensation are operated in parallel, the primary requirement of their automatic-control equipment is that it insure stability of operation.[8] Stability can be achieved automatically either by electrical or mechanical interconnections which cause all of the load-ratio-control mechanisms to remain in step with one another under the control of a single set of automatic equipment, or by a control arrangement in which any circulating current that does arise is made to react on the automatic-control equipment in each unit in such a way as to be self-reducing.[9] This latter method is usually to be preferred inasmuch as it offers greater operating flexibility and also corrects automatically for circulating current that might be caused by differences in the supply voltage to the various transformers.

In addition to the basic requirement of stability, experience has shown that it is usually very desirable to incorporate other features in the automatic control equipment, if complete and successful automatic performance is to be expected under all normally encountered operating conditions, particularly where the substations are unattended.

Maintenance of Correct Load-Center Voltage as Number of Transformers in Service Is Varied

It is the function of the line-drop compensators to indicate to the automatic regulating equipments the amount by which the bus voltage must be altered in order to maintain the load-center voltage at the desired level. This requires that the compensators receive a true indication of the magnitude of the load current. When the number of paralleled transformers is varied, as for example by removing one or more from service during periods of light load, special provision is necessary if the correct load-center volt-

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S. Minneci and S. B. Farnham are both with the General Electric Company, Mr. Minneci is electrical engineer in the power transformer division at Pittsfield, Mass., and Mr. Farnham is in the central station engineering department at Schenectady, N. Y.

age level is to be preserved without requiring the attention of an operator. The need for such provision may be illustrated by the case of two load-ratio-control transformers operating in parallel and sharing a common remote load, as illustrated by the single-line diagram of Figure 1. With their line-drop compensators set to hold the desired load-center voltage, assume that one of the transformers is removed from service. The load current which was passing through it is thereby diverted through the other. This additional current reacts on the line-drop compensator and automatic-control devices of the transformer remaining in service to raise the bus voltage to a higher level than was being held while both units were in service. In other words, the removal of one transformer from service has the same effect on the line-drop compensator and automatic-control equipment of the other, as if the load had increased, and the bus voltage is raised accordingly. Since the actual line drop between the bus and the load center has not increased, however, the load-center voltage is raised above its normal level.

It will be apparent that this effect is most pronounced when only two units are operated in parallel. As the number of paralleled units is increased, the removal of any one from service produces proportionately less increase in current through the remaining units. However, the removal from service of any substantial portion of the total number of units will cause an appreciable rise in the voltage level; in any case, corrective measures are desirable.

Avoidance of Excessive Initial Circulating Current at the Instant of Paralleling

Unless means are included for bringing the load-ratio-control mechanism of an idle transformer to a position approximating the existing bus voltage level, there may be an excessive initial circulating current when the paralleling circuit breaker is closed. The magnitude of the initial circulating current is a function of the impedance in the loop circuit comprising the load-ratio-control transformers and the interconnecting circuits on both the high-voltage and low-voltage sides, and also of the voltage introduced in the loop by the difference in position of the voltage-regulating mechanisms. In the usual case, the predominant impedance is that of the transformers themselves. The high-voltage and the low-voltage interconnections usually add little impedance, particularly in the case of double-ended unit substations, where the transformers connect directly to a common load bus, or in the case of single-transformer unit substations joined in parallel through short interconnecting circuits. The units are sometimes connected to a common high-voltage circuit, although if each is supplied over a separate circuit, the total loop impedance is usually not increased materially, as the impedance of the high-voltage lines when reduced to a per-unit basis, is generally relatively small.

The maximum circulating current, of course, occurs when a unit whose voltage-

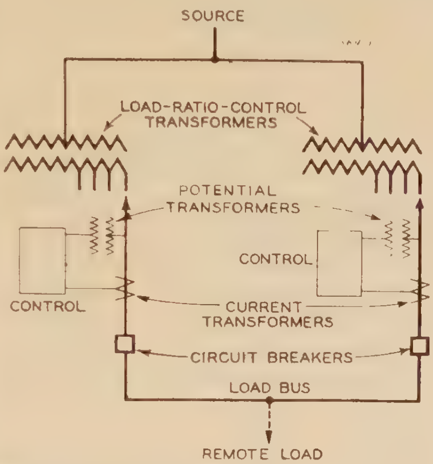

Figure 1. One-line diagram of three-phase load-ratio-control transformers connected in parallel

Automatic-control equipment energized from one phase of each transformer circuit

regulating mechanism is at one extreme end of its range is paralleled with others whose mechanisms are at the opposite end. It is evident from this that the possibility of trouble arising from initial circulating current becomes greater as the range of the voltage-regulating equipment is increased. As an indication of the order of magnitude of the circulating current, consider as a typical case the two transformers illustrated in Figure 1. If each has a 20 per cent range of load-ratio control, they can introduce a maximum voltage of 20 per cent in the loop circuit when one is at the full raise position and the other at the full lower position. Then, assuming that each has an impedance of six per cent, and that the impedance of the interconnecting circuits is negligible, the maximum circulating current is 20/12, or 1.67 times normal full load current of one unit. This circulating current is essentially a zero power-factor current, and adds to the load current at an angle approaching 90 degrees.

While excessive initial circulating current can be avoided if the voltage-regulating mechanisms are brought to the same voltage position manually, experience has shown that system operators are usually unwilling to accept this arrangement; and where remote operation or supervisory control over the paralleling circuit breaker is involved, the means for avoiding excessive initial circulating current must be fully automatic almost invariably.

In the new control system recently developed and described in this paper, great flexibility in operation is obtained by incorporating features that will:

(*a*). Insure stable operation during parallel operation.

(*b*). Maintain the correct load-center voltage as the number of transformers in service is varied.

(*c*). Avoid excessive initial circulating current at the instant of paralleling.

(*d*). Permit independent, nonparallel operation when desired.

The various voltages and currents referred to, unless otherwise specified, are those which appear in the secondary windings of potential and current transformers and which are measures of the true values in the power circuit.

The Control Equipment

Each automatic load-ratio-control transformer possesses and is controlled by its own complete set of control devices, including a contact-making voltmeter, line-drop compensator, potential transformer, and current transformer, permitting it, therefore, to operate as an individual unit.

For the purpose of reducing circulating current during parallel operation, each unit is provided with a circulating current compensator and an auxiliary current transformer. In addition, in order to maintain the correct load-center voltage level as the number of units operating in parallel is varied, and to permit the avoidance of an excessive initial circulating current, a second auxiliary current transformer is provided with each unit. Since these two auxiliary current transformers and the circulating current compensator operate in the secondary circuit of the main current transformer, they are physically so small that they may be mounted usually on the control panel. Only five external control wires are required for the interconnections between the units, regardless of the number of load-ratio-control transformers that are to operate in parallel.

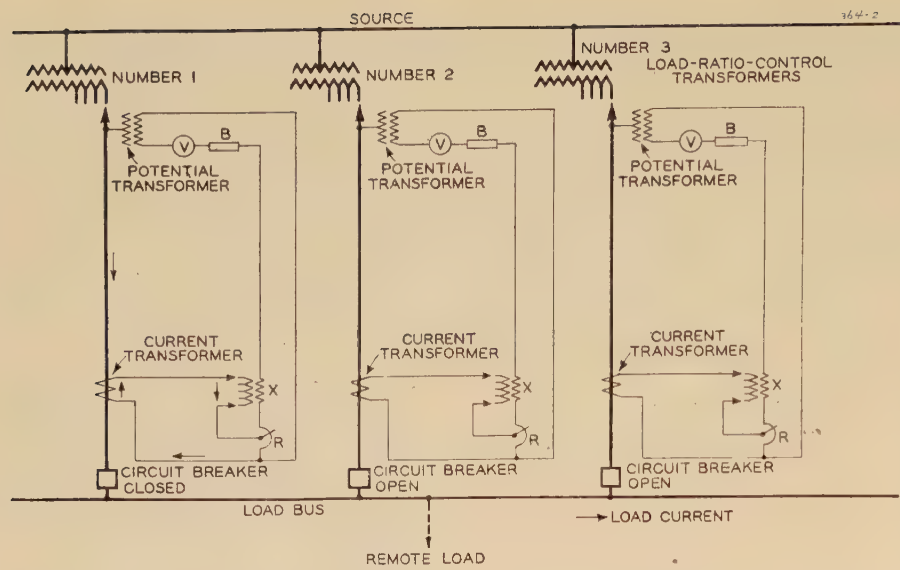

Figure 2. Control arrangement suitable only for individual operation

Principle of Operation

The connections of the control circuits between the several paralleled units, in conjunction with the appropriate connections of the circulating-current compensators and auxiliary current transformers in each unit are such that:

(*a*). Circulating current between transformers does not result in corresponding current in the line-drop compensators. This prevents the line-drop compensation from being influenced by circulating current. The line-drop compensators, therefore, respond only to load current.

(*b*). The load current in each line-drop compensator is always proportional to its transformer's share of the total load current, regardless of whether or not that particular transformer is in service. To accomplish this, the line-drop compensators on all units—whether in service or idle—are kept energized, and equal amounts of the load current flow through all of the compensators at all times.

In the case of transformers of unlike kilovolt-ampere ratings operating in parallel, the currents in the line-drop compensators will be equalized when the load current is divided among the transformers in proportion to their kilovolt-ampere ratings.

Functions of the Circulating-Current Compensator and Auxiliary Current Transformers

One auxiliary current transformer functions to separate the load current from the circulating current. The circulating current thus segregated is, by appropriate connections, forced through the circulating-current compensators of all units in service. A reactive voltage proportional to the circulating current is thereby inserted in the contact-making voltmeter circuit. The circulating-current compensator, sometimes referred to as a "paralleling reactor" is an iron-core reactor with two windings. One winding carries the circulating current, and the other is connected in series with the contact-making voltmeter. It is provided with taps for adjusting the magnitude of the voltage introduced in the voltmeter circuit for a given amount of circulating current.

The second auxiliary current transformer forces an equal division of load current through the line-drop compensators of all the units, regardless of whether they are in service or idle.

Operation of the Control Circuit

To facilitate an understanding of the manner of operation of the control system in which the foregoing features are incorporated, the relatively simple control arrangement for automatic operation of individual units is first considered. Figure 2 is a one-line diagram representing several three-phase load-ratio-control transformers, each with its own set of control equipment. The control circuits are separated from one another, and will be recognized as the arrangement normally used for independent, nonparallel operation, wherein each contact-making voltmeter and line-drop compensator is energized by its own potential and current transformer. With this arrangement, however, only one unit at a time may be connected to the load bus because, if one or more units are added in parallel the presence of any circulating current in the line-drop compensators will cause a further increase in the voltage delivered to the bus by the transformer whose voltage is already high, and a decrease in the voltage of the transformer whose voltage is already low. This results in unstable operation, with one unit going to the maximum raise position and the other going to the maximum lower position, thereby increasing the circulating current.

Circulating-Current Compensation

The circulating current, however, can for all practical purpose, be eliminated

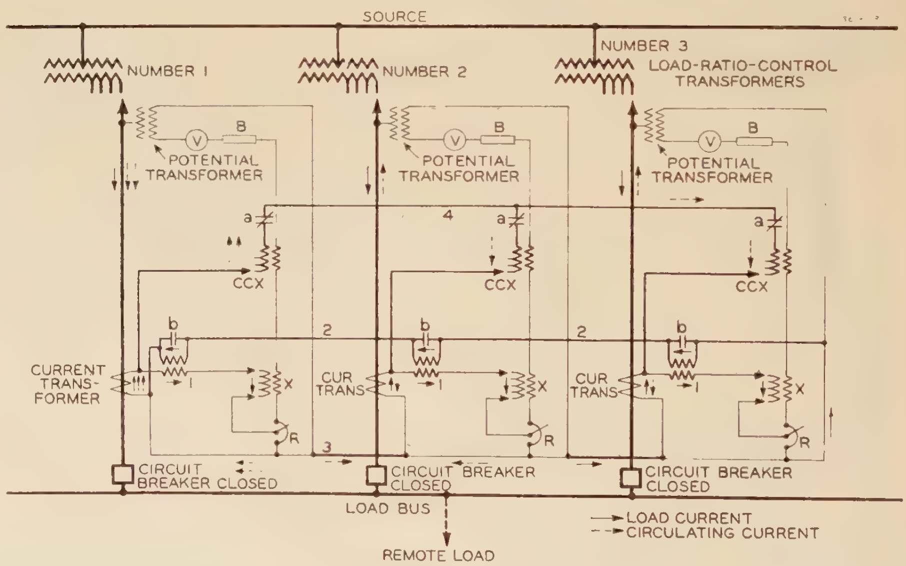

Figure 3. Control arrangement in which load current and circulating current are segregated

The load current indicated by solid arrows flows through line-drop compensators *R* and *X*, while the circulating current indicated by broken arrows flows through circulating-current compensators *CCX*

by inserting in the control circuit of Figure 3 the auxiliary current transformers 1, which separate the load current from the circulating current, and the circulating-current compensator *CCX*. In this figure the three transformers are shown connected in parallel to the load

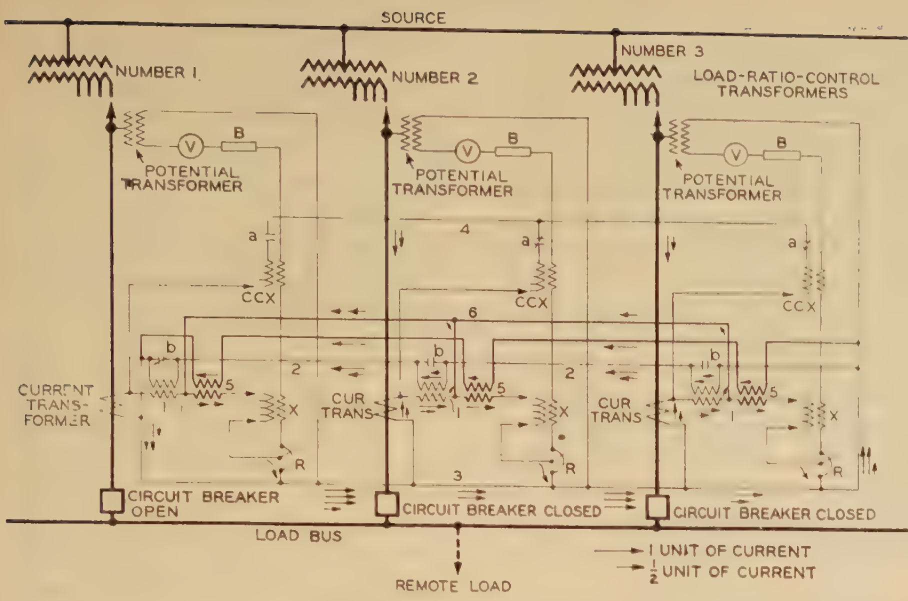

Figure 4. Control arrangement showing the addition of auxiliary current transformers 5 for effecting the proper division of load current among the line-drop compensators when transformers are removed from service

bus. Under balanced conditions, load current flows in the main current transformers and line-drop compensators, as well as in the auxiliary current transformers 1 as indicated by the solid arrows. Since these auxiliary current transformers are connected in series with each other through control wires 2 and 3, equal amounts of load current flow through all the line-drop compensators, thereby introducing the proper amount of voltage compensation in the contact-making voltmeter circuits.

Circulating current appearing in the secondary windings of the main current transformers, however, is prevented from flowing in the afore-mentioned loop by the polarity of the connections, but is forced to flow through the circulating-current compensators *CCX* and control circuits 3 and 4 as indicated by the broken arrows. It will be noted in Figure 3 that the flow of circulating current in the power circuits is from transformer number 1 toward the load bus, returning through transformers numbers 2 and 3. Accordingly, the current in *CCX* for transformer number 1 is in the direction to lower its output voltage, while in the *CCX* elements of transformers numbers 2 and 3 it is in the opposite direction and tends to raise the output voltage. The net effect is the reduction of the circulating current.

The vector relationship of the circulating-current compensation with respect to the voltage applied to the contact-making voltmeter is shown in Figure 6.

Auxiliary switches on the circuit breakers are used to change the control connections automatically when any transformer is removed from service. These are designated as *a* and *b* in Figure 3. When a transformer circuit breaker is opened, auxiliary switch *a* isolates the circulating-current compensator *CCX*, while switch *b* by-passes the auxiliary current transformer 1, thereby permitting the correct compensation for circulating current among the transformers remaining in parallel operation.

If for any reason the controls are prevented from correcting for circulating current, an overcurrent lockout relay (not shown in Figure 3) sometimes is provided to stop operation of the load-ratio-control motor before the current reaches an unsafe value. The relay operating coil is usually connected in series with the *CCX* element where it is responsive to circulating current only, and is, therefore, free from the influence of load current. When the power circuit breakers are equipped with current directional relays for other reasons, as is the normal practice in the case of unit substation, the lockout relay may be omitted. In these cases, the breaker is tripped automatically when the circulating current reaches the value corresponding to the relay setting.[10] The circulating current is thereby removed entirely rather than merely prevented from increasing further.

Equalization of Current in the Line-Drop Compensators

To permit varying the number of transformers in service without affecting the line-drop compensation, a previously mentioned second auxiliary current transformer 5 is added to each equipment, as shown in Figure 4. The manner in which this causes an equal division of load current among all the line-drop compensators will be apparent from the circuit of Figure 4, wherein transformer number 1 is removed from service as indicated by the open position of its circuit breaker.

If it be assumed that three units of current represents the total load current supplied to the load bus by transformers numbers 2 and 3, then each carries $1^1/_2$ units of current. It is apparent that if $1^1/_2$ units of current are allowed to flow through their line-drop compensators, these transformers will raise the bus voltage to a higher level than was being held before the breaker of number 1 transformer was opened, since previously only one unit of current flowed in each compensator. The presence of the auxiliary current transformers 5 however, permits only one unit of current to flow in each of these line-drop compensators, while the excess half units are forced to flow in control wire 6, and their sum—one unit—flows through the line-drop compensator of the idle transformer number 1. The line-drop compensation of all the transformers, therefore, remains at exactly the same value at all times, thereby maintaining the correct bus and load-center voltage levels regardless of the number of transformers in service.

Although in the figures used for illustration, only three transformer units are shown, the complete symmetry of the arrangements makes it apparent that this scheme is applicable to any number. If there are N transformers, X of which are operating in parallel, and if unit current flows in each when $X = N$, then the division of load current among the paralleled transformers will be in proposition to N/X. Only unit current, however, is allowed by the auxiliary current transformers 5 to flow in each of their line-drop compensators, and the excess, which is equal to $(N/X)-1$ or $(N-X)/X$ for each transformer, is diverted to the $N-X$ idle transformers. This results in a total current proportional to $[(N-X)/X](X)$ being divided among $N-X$ idle transformers with the result that each idle transformer also has unit current in its line-drop compensator.

Circuit-Breaker Closing Circuit

To avoid an excessive initial circulating current at the instant of paralleling, advantage is taken of the fact that the correct share of load current is automatically maintained in the line-drop compensators of the idle as well as the active transformers. This makes possible the automatic

equalization of the output voltage with the existing bus voltage, so that when operating conditions require, any idle transformer may again be connected to the bus, with the assurance that its voltage will be of the correct value to permit paralleling without causing a large initial circulating current. A slight phase displacement will exist between voltages as a result of the impedance drop in the loaded transformers, but in the usual case this should not be objectionable.

Figure 5 is a partial elementary diagram showing the salient features of a circuit-breaker closing circuit which permits the automatic voltage equalization

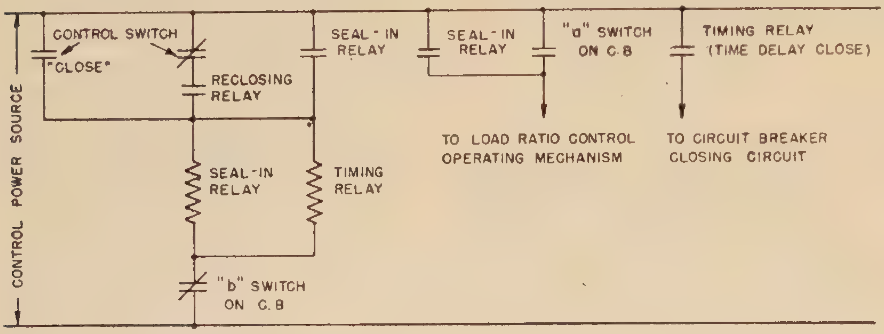

Figure 5. Partial elementary diagram of control circuit for permitting equalization of voltages before closing the circuit breaker

to take place before allowing the breaker to close. In this circuit, the only devices required in addition to those normally employed are a timing relay and an auxiliary seal-in relay. The manner of operation may be seen readily by reference to Figure 5. When the control switch is turned to closed, or when the reclosing relay (if present) operates, the seal-in relay is picked up and sealed. Immediately the load-ratio-control motor circuit is energized and the timing relay is started. This relay has a time interval which is sufficient to permit the load-ratio-control mechanism to reach the position corresponding to the existing bus voltage. A true indication of the bus voltage, as has been shown, is given to the contact making voltmeter of the idle unit by the potential transformer connected to its own output circuit, and its line-drop compensator. At the expiration of the timing interval, the circuit breaker closing mechanism is energized, and the breaker closes. While it is probable that in most cases the idle load-ratio-control mechanism will have assumed the correct voltage position before the interval elapses, there is the possibility that it may occasionally have to traverse its entire range before the correct position is reached, and the timing interval should be sufficient to allow this.

After the circuit breaker is closed, the seal-in relay and timing relay are deenergized by means of an auxiliary switch. The load-ratio-control circuits, however, are kept energized through another auxiliary switch so that further voltage changes can be made as required by system conditions.

In the foregoing it is assumed that paralleling is accomplished by closing the circuit breaker between the load-ratio-control transformer and the load bus, as illustrated in Figures 1 through 4. If in the actual installation there is also a switch or circuit breaker between the transformer and the source, this must be closed first in order that the automatic voltage equalization may occur.

Individual Operation

Since each load-ratio-control transformer is provided with a complete set of automatic-control equipment, any of the transformers shown in Figure 4 may be operated independently. It is necessary only to open control wires 4 and 6 and to by-pass the auxiliary current transformers 1 and 5, thereby reducing the interconnected control arrangement of Figure 4 to the simple isolated arrangement of Figure 2. For this purpose a control switch (not shown) may be used, or if the change from parallel to individual operation is to be made only very infrequently, the necessary changes in the control-circuit connections may be made directly at the terminal boards of the control equipment.

Summary

This new control system provides a means whereby all of the features may be secured which appear desirable in applications requiring the fully automatic parallel operation of load-ratio-control transformers. Its outstanding characteristics are these:

1. Accurately regulated voltage is maintained at the remote load center through line-drop compensation which is responsive to load current only.

2. Circulating current—regardless of its cause—is self-reducing, thereby insuring stable operation.

3. Both the voltage level and the circulating-current compensation are independently adjustable.

4. Bus and load-center voltage levels are automatically maintained at the correct values when transformers are added to or removed from parallel operation.

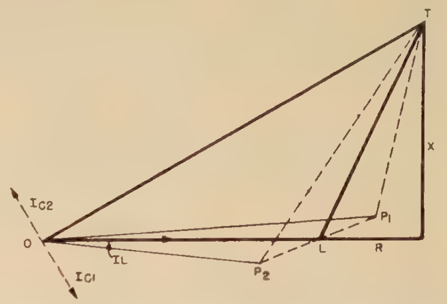

Figure 6. Vector diagram illustrating the corrective voltages impressed on the contact-making voltmeters as a result of circulating current

O-T—Voltage at transformer load bus

O-L—Voltage at remote load center and at each contact-making voltmeter for normal conditions. (No circulating current)

I_L—Load current, 100 per cent power factor

Ic_1—Circulating current through unit 1: lags *O-T* by an angle approaching 90 degrees

Ic_2—Circulating current through unit 2: leads *O-T* by an angle approaching 90 degrees

O-P_1—Voltage at contact-making voltmeter on unit 1 when circulating current exists

O-P_2—Voltage at contact-making voltmeter on unit 2 when circulating current exists

R-X—Resistance and reactance voltage components in line-drop compensator

T-L—Total line-drop compensation for normal conditions

L-P_1, *L-*P_2—Voltage compensation resulting from circulating current

T-P_1—Total impedance drop in series with contact-making voltmeter on unit 1

T-P_2—Total impedance drop in series with contact-making voltmeter on unit 2

5. In order to avoid the occurrence of a large initial circulating current, the output voltage of any idle transformer is automatically equalized with the existing bus voltage before permitting the closing of the paralleling circuit breaker.

6. Any number of load-ratio-control transformers may be operated in parallel, and new transformers may be added without requiring modification of the control equipment on those already in service.

7. Operating flexibility is inherent in this "unit" arrangement which permits either parallel or individual operation.

Appendix

Circulating-Current Voltage Compensation

The effect of circulating current on the voltage impressed on the contact-making voltmeters is shown in Figure 6 for the case of two load-ratio-control transformers operating in parallel. The vector diagram is drawn for the case of unity power-factor load current, and shows the conditions which exist before the circulating current is reduced. No attempt is made to indicate the precise magnitudes of the various components as the diagram is intended merely to illustrate their general relationship.

Definition of Symbols

PT—Potential transformer.
CT—Current transformer.
CB—Circuit breaker.
R—Resistance component in line-drop compensator.
X—Reactance component in line-drop compensator.
V—Contact-making voltmeter.
B—Ballast for contact-making voltmeter.
CCX—Circulating-current compensator.
a—Auxiliary switch on circuit breaker. Closed when circuit breaker is closed.
b—Auxiliary switch on circuit breaker Closed when circuit breaker is open.
$\neq$—Contacts closed.
$=$—Contacts open.

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Train Communication

L. O. GRONDAHL
MEMBER AIEE

P. N. BOSSART
NONMEMBER AIEE

THE communication system that is used in the operation of railroads has developed very rapidly in the last few years. The most recent addition is what is known as train communication, which is a system of communication for operating purposes between railway vehicles and between such vehicles and fixed points. To illustrate, train communication equipment may be used for two-way telephone or code communication between the two ends of a train, between trains and stations, between trains and towers, between two parts of a train that are separated for work or otherwise, between two different trains whether they are on the same or on adjacent tracks, and between the conductor and locomotives in a receiving or in a classification yard. Following are illustrations of the uses to which such equipment may be put:

A. Communication Between Vehicles in the Same Train. This is most important in long freight trains in which it is very difficult to signal between members of the crew. There are many possible applications:

1. The conductor may call for an air-brake test before starting the train and may report to the engineer on the progress of the test, thus decreasing the amount of time required.

2. The conductor can notify the engineman that everyone is on board.

3. He can request the engineman to increase or decrease speed as desired or required.

4. The crews at the two ends of the train can compare train orders received while in motion.

5. Information in regard to change in work to be done at work points can be exchanged.

6. The conductor can give information enabling the engineman to spot the caboose at any desired point, for instance when it is necessary to clear road crossings or switches.

7. When in motion the conductor can report to the engineman dragging brakes, hot boxes, or any other condition that makes it necessary to stop the train, and arrange for the best time and place to stop.

8. The apparatus may be used to obtain the proper co-operation between the hauling and the pushing locomotives on a long train where two or more locomotives are co-operating.

9. The locomotive crew can give information to the crew at the rear of the train concerning causes for delay and estimates of the duration of the delay.

10. The conductor can give instructions to the engineman as to procedure under unusual conditions of any kind without having to walk to the front end of the train.

B. Communication Between Trains. When two trains are passing, the crew of one train may notice something that should be corrected on the other train, and with the communication system it is possible to report such conditions, for instance, as the existence of hot boxes or dragging brake rigging or shifting loads.

C. Communication Between Train and Station or Between Train and Tower. Ability to communicate between trains and stations or towers is valuable first of all in that it enables the towerman to transmit messages to a train crew without stopping the train or reducing its speed. It is important also from the standpoint that it enables the train crew to report unusual road conditions to the towermen.

D. Communication in Hump Yards. In train classification the communication system is a very great convenience in that it enables the conductor, who is usually located at the hump of the yard, to communicate with his locomotives at all times and to tell them what to do, which string of cars to move, when to start, how fast to move, and when to stop. This is useful on the classification side of a hump, as well as on the receiving side.

The applications are so many and so important that one can almost say that in common with all communication equipment, as soon as the equipment becomes available, it also becomes indispensable.

Previously Proposed Methods

Several methods of providing train communication have been proposed. Among the first was the suggestion that the air-brake system might be used as a speaking tube. Investigation revealed that the brake air line was an extremely

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L. O. Grondahl is director, research and engineering, and P. N. Bossart is research engineer, both with the Union Switch and Signal Company, Swissvale, Pa.

The material of the paper is the result of the work of a great number of past and present members of the research department of the Union Smith and Signal Company, especially past members Austin M. Cravath and John H. Findlay.

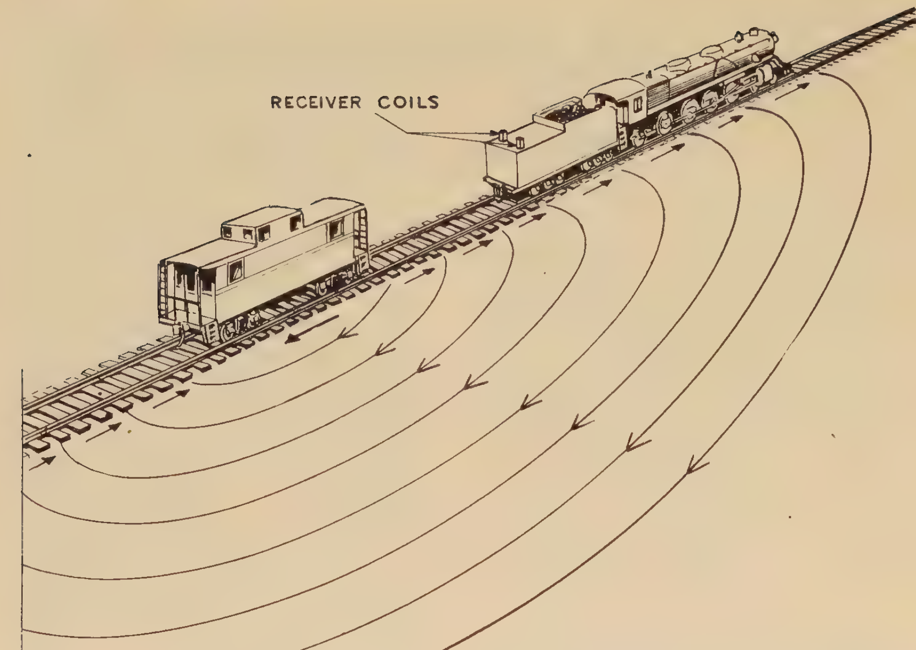


Figure 1. Current flow, caboose to locomotive

efficient low pass filter—so good, in fact, that no frequencies higher than about one cycle per minute could be passed through the brake pipe of a 150-car train. Even this signal could not be transmitted satisfactorily under certain conditions of the brake line, and the extremely low speed of signaling made the system impractical since a single signal would have to combine several pulses and might take five minutes to complete.

Another suggestion required the wiring of all freight cars. The wires were to be connected automatically through the brake line couplers, and there is no doubt that if such a circuit were established, communication over it would be easy. There is the consideration that the wiring of two million freight cars is a large undertaking, and even more important, that both this method and the signaling through the brake pipe have the fundamental limitation that neither can be used if the train is broken. This in itself renders both systems impractical, since communication is often most needed when the train is parted.

Radio has been proposed and tried on many occasions, and it is possible by radio to obtain communication, especially between ends of a moving train. Communication is uncertain in tunnels and fades under steel bridges and similar structures. This condition makes radio unsatisfactory for the purpose. The most important difficulty with radio is that the Federal Communications Commission has not been able to make permanent assignments of wave bands that are suitable. They have to be reserved for the use of services which have no alternative means of communication, such as ships and aircraft.

Commercial Method

The Union Train Communication System, a development of the Union Switch and Signal Company, is in regular commercial use and is being adopted rapidly in railway applications. It is a carrier telephone system transmitting usually the upper side band of a 5,700-cycle carrier and ordinarily feeding the signals conductively into the rails and picking them up inductively from the rails. Figure 1 shows the principle of the system. At the sending vehicle a voltage is produced along both rails in parallel to send out currents through the running rails with the return path through the ground. The signal is picked up at the receiving end by coils in inductive relation to the rails and is amplified and demodulated for reception in the loud-speaker. The apparatus has been developed as a simplex or "press-to-talk" system.

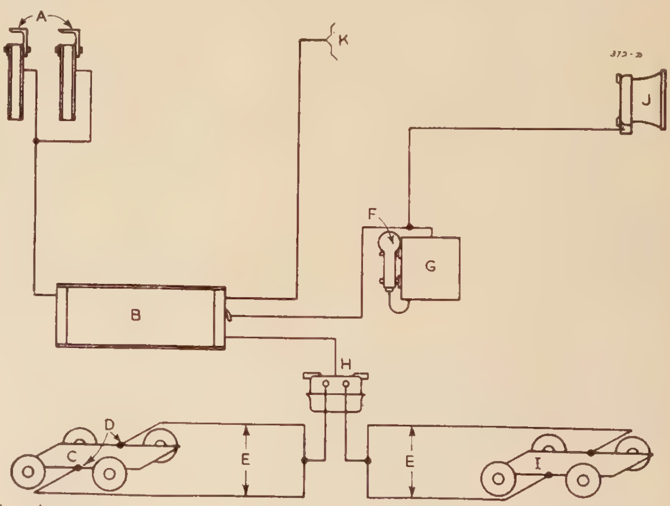


Figure 2. Block diagram of two-way equipment on caboose

A. Receiving coils (located in inductive relation to rails)
B. Equipment box with transmitter-receiver and dynamotor
C. Caboose rear truck (insulated from caboose frame)
D. Connections bolted to truck frame
E. One-inch copper pipe
F. Hand microphone or handset
G. Control panel
H. Output transformer unit
I. Caboose front truck (insulated from caboose frame)
J. Loud-speaker
K. 32 volts direct current from caboose battery: 100 watts receiving; 500 watts sending

Description of Apparatus

Figure 2 shows the essential parts of a typical equipment. The transmitting circuit is a loop from the insulated truck at one end of the vehicle through the output transformer with tuning condenser to the truck at the other end of the vehicle with the loop completed through the running rails between the wheels of the trucks to which the connections are made. The impedance drop in these rails is the transmitting rail voltage. Speech is transmitted by a telephone-type transmitter and received either over a loud-speaker or over the receiver of a handset.

In Figure 3 two indication lights are shown at the top of the control panel, one showing red when the 32-volt d-c power is on, and the other flickering white with the modulation of the voice to assure the speaker that his message is being transmitted to the rails. The button beneath the lights is used for sending a calling signal which is received as a steady 1,050-cycle note and is sent as a single frequency 1,050 cycles above the carrier. The knob at the bottom of the panel is for the control of the received volume.

In spite of the size of a locomotive, space in the locomotive cab is at a premium, and it is sometimes difficult to find a

place for the speaker and the control box. The speaker may be located against the roof pointing down, and the control panel back of the engineman.

In Figure 4 the transmitter and receiver combined in one frame are at the left, and the 32-volt dynamotor, rated 500 milliamperes 400 volts d-c, combined with switching relay and ripple filters are in the frame at the right.

An optional piece of equipment, sometimes used, is the "signal selector." It includes additional amplification and sharp filtering for the demodulated calling signal which operates a relay. It cannot be operated by short pulses or continuous wide band interference because of a combination of saturation of the tube, time delay in the relays, and narrow band of response. When this equipment is used for calling only, the loud-speaker is disconnected from the amplifier in the stand-by position, and the calling signal operates the bell. When used with signal lights for proceed and stop indications the loud-speaker is disconnected, and a green or proceed light shows as long as the calling signal is being received, but when the calling signal stops, the bell rings, a red light shows, and the loud-speaker is connected ready to receive speech. This provides a proceed indication on the so-called "closed circuit principle."

Figure 6 shows a portable receiving amplifier used on locomotives in hump yards for one-way communication. It weighs about 50 pounds. The circuits are essentially the same as those of the receiver for two-way communication, a 32-volt vibrator furnishing the plate voltage. In addition to the portable box shown, engine equipment for one-way communication involves a portable coil for picking up the signal from the rails and a portable loud-speaker and attached volume control for receiving messages in the engine cab. This equipment can be changed from one engine to another in a few minutes.

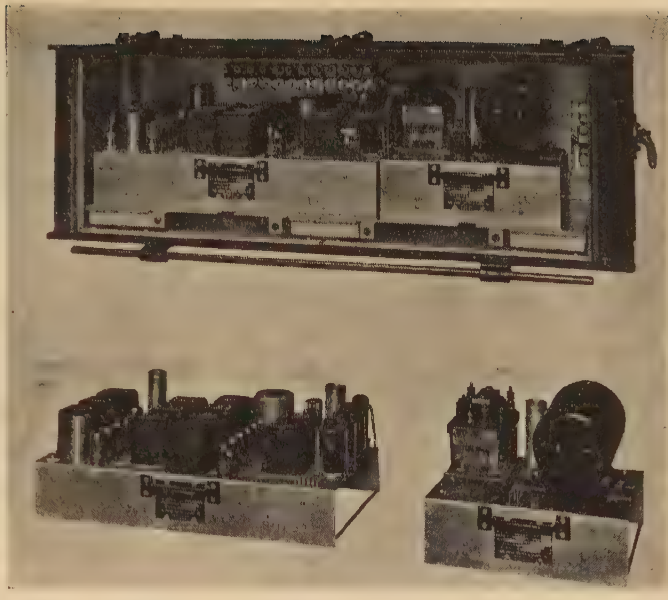


Figure 4. Open equipment box, showing at the top, frames in position in box, and below, frames when removed

Equipment Circuits

Figure 7 shows the circuit diagram. The signal is picked up by the receiving coils at top left, amplified, as a single side band through two stages of carrier frequency amplification, and then demodulated by a pair of triodes. Some of the carrier frequency from the oscillator is fed into the demodulator tubes in parallel while the single side band is applied in push–pull. Any undesired high-frequency components are removed by the carrier suppression filter which follows. The output of this filter is applied to the grid of a power tube, and its output feeds the loud-speaker or a handset.

Automatic volume control is provided by the tube at the extreme left where the right half of the twin triode amplifies the demodulated signal taken from the filter input. When full output is reached by the output tube, the peak of this amplified voltage begins to exceed the positive voltage provided from D to ground, and the volume control starts to act by allowing the left half of the tube to rectify and charge the RC circuit connected so as to apply negative bias to the grids of the two carrier frequency amplifiers. The RC circuit provides considerable time delay. This is necessary since, with no carrier, transmitted volume control must be obtained from a time average of the amplitudes of the speech frequencies. Once the volume control begins to act, the output from the last tube stays ap-

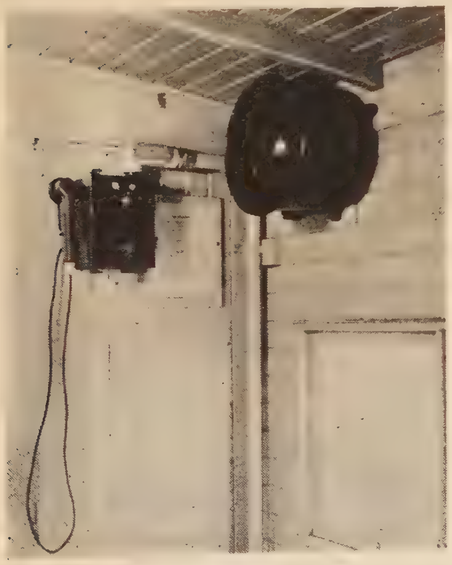


Figure 3. Control box with hand microphone and loud-speaker in caboose

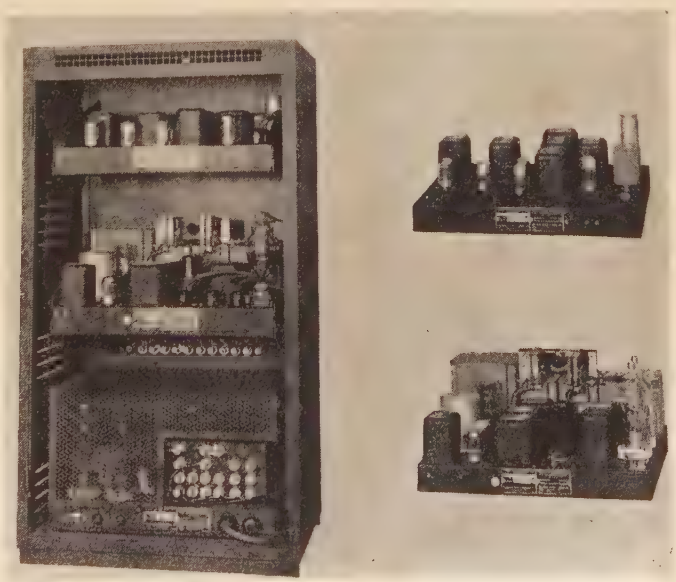


Figure 5. Cabinet for wayside station with receiver on top shelf, transmitter on middle shelf, and power supply at bottom, arranged for 110-volt 60-cycle supply

proximately constant, and the desired level in the loud-speaker or handset is set by a manual potential divider or volume control across the output.

Short high interference peaks are cut off by the peak limiter at an amplitude no higher than required for normal signals to load the output tube. This is a twin triode connected to short-circuit at the filter output the two halves of any amplitude greater than normal, and it operates after the *AVC* adjusts the gain. Normal peaks are set by two sliding contacts on the cathode resistor of the output tube.

In the transmitter the output of a telephone-type microphone is applied to the grids of the modulator stage in push–pull while the carrier energy is applied to the grids in parallel. The resulting modulation, if the circuit is perfectly balanced, does not contain the carrier but does have in it principally the two side bands. Any remaining carrier and the lower side band are removed by the filter which follows, so that practically the tubes which drive the power stage are energized by the upper side band only. The output stage consists of four 6*L*6 tubes in parallel push–pull and is connected to the sending loop or direct coupling circuit shown in the figure which extends from the engine pilot truck wheels through the rails to the drivers and back through the engine frame.

Transmission Through Track

It is interesting to examine the transmitting characteristics of a railroad track with ground return. The characteristics of any infinite transmission line with distributed constants are determined by four fundamental constants, namely:

R, the series resistance per unit length
X, the series reactance per unit length
G, the shunt conductance per unit length
B, the shunt susceptance per unit length

Approximate values of these quantities for two rails in parallel at 8,000 cycles per second are as follows:

$R=1.25$ ohms per thousand feet
$X=12.5$ ohms per thousand feet
$G=1$ mho per thousand feet (for 3-ohm ballast resistance)*
B is negligible compared to conductance G.

Other symbols used are:

$Z=R+jX$—series impedance per unit length

* Ballast resistance, a common term in railway signaling, is the resistance between the two rails of a unit length of track, 1,000 feet if not otherwise specified. Measurements show the resistance of two rails in parallel to ground is approximately one-third instead of one-fourth the resistance from rail to rail. The reason for this is that some current, with a voltage between rails, flows along the ties and through the ballast without going to ground.

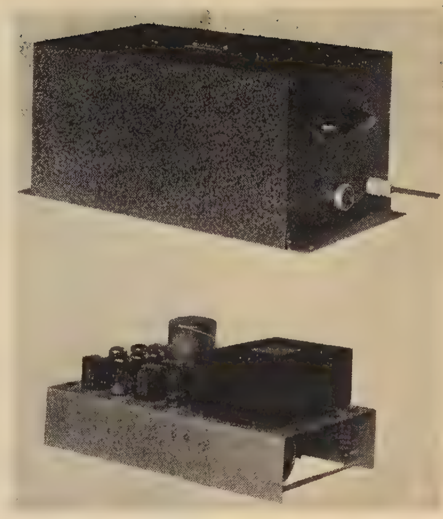


Figure 6. At top, portable receiver for one-way communication. Its chassis is shown removed, below

$Y=G+jB$—shunt admittance per unit length
Z_o=characteristic impedance
$\gamma=\alpha+j\beta$=transmission constant
α =attenuation constant
β =phase constant

We can apply the formula for the characteristic impedance of a transmission line

$$Z_o=[(R+jX)/(G+jB)]^{1/2}$$

or we can write it as the series impedance and the shunt conductance

$$Z_o=[Z\underline{/\theta}/G\underline{/0}]^{1/2}$$

As we substitute the values chosen we have

$$Z_o=[12.5\underline{/84^\circ}/1\underline{/0^\circ}]^{1/2} =3.5\underline{/42^\circ}\text{ ohms}$$

This means that under the conditions assumed, the impedance presented by a railroad track with three ohms ballast resistance is about 3.5 ohms in one direction for an infinite track.

The transmission characteristics are obtained from the equation

$$\gamma=[Z\underline{/\theta}\cdot G\underline{/0}]^{1/2} =[12.5\underline{/84^\circ}\cdot 1\underline{/0^\circ}]^{1/2} =3.5\underline{/42^\circ}$$

or

$$\alpha+j\beta=2.6+j2.35$$

Thus, if we start with a current I_o of value 1 at the transmitter, the current will fall to the value $e^{-\alpha x}$ at distance from the transmitter equal to x in thousands of feet, or for the value given, the current will fall to half value in 270 feet. The phase shift is β radians per thousand feet and the wave length, $\lambda = 2\pi/\beta$, or distance for phase to be retarded a complete cycle, is in this case about 2,700 feet.

The equations show that if the conductance, G, is one fourth as great, corresponding to 12 ohms ballast resistance, the characteristic impedance Z_o is twice as great, and the attenuation and phase shift are half as much. In this case,

$Z_o=7\underline{/42^\circ}$ ohms
$\lambda=5{,}400$ feet
$x=540$ feet (for $I=I_o/2$)

Range

The current levels involved in train communication are of interest. The current in the rails near the sending vehicle is of the order of $^1/_2$ to 1 ampere. The noise level in a quiet yard track is about 10^{-7} amperes. For communication to be intelligible, it is necessary that the signal should be received at a current level about four times that of the noise level. It follows that the ability to receive a signal with about one-half microampere in the rails represents the maximum usable sensitivity of the amplifier. On an ordinary main-line track of a railroad in a rural region, the noise level is about 10^{-5} amperes. On main-line track through an industrial region, the noise level may be about 10^{-4} amperes. This is measured by noting the noise current through the loud-speaker and then, with noise removed, adjusting and measuring the rail current of a 1,000-cycle modulated upper side band to give the same loud-speaker current. If we take these values and put them into our expression for track attenuation, we find that with 12 ohms ballast resistance, a signal can be received through the track alone, without line wires, at a distance of about $1^1/_2$ miles on main-line track in relatively quiet areas. With paralleling line wires present, speech can be exchanged at 100 miles between a wayside station and a train, and at ten miles or more between moving vehicles.

Choice of Carrier Frequency

If we look at the expression for the attenuation of the track current, we see that it varies directly as the square root of the frequency, assuming that the series impedance of the track Z varies directly as the frequency, and the shunt conductance is independent of frequency, which is very nearly true in the region of interest. Hence, the frequency must not be too high. The frequency must not be too low, because the noise level in the track is much greater for low frequencies. Experiments were made with voice frequen-

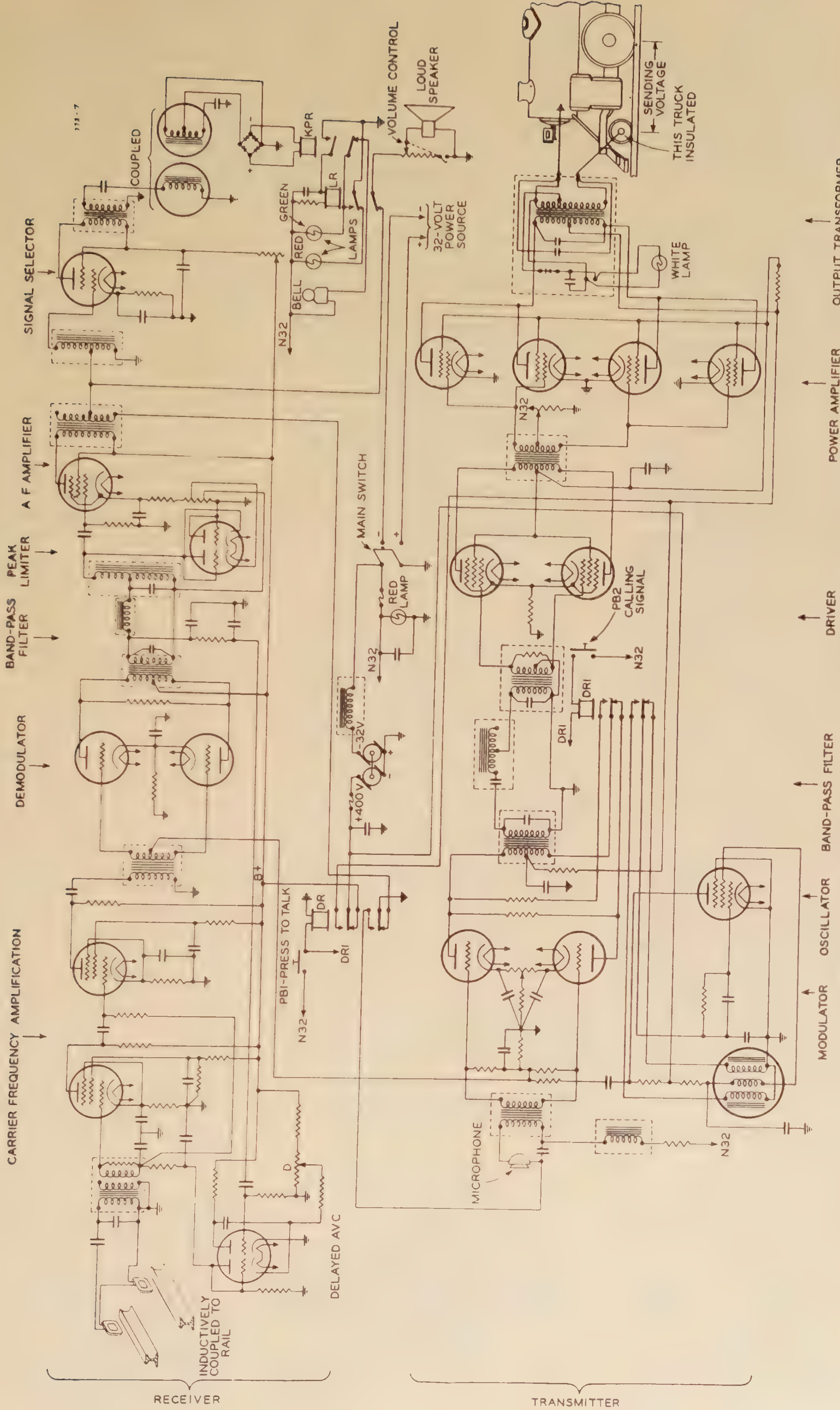


Figure 7. Circuit diagram for two-way train-carried equipment with signal selector

cies, but this was impractical for two reasons:

1. Because of the high noise level which contains large peaks because of power-frequency harmonics and commutator ripples from rotating machinery.

2. Because communication at this frequency interferes with paralleling telephone lines.

These conditions require the use of a frequency above 3,000 cycles per second, and the band between power frequency and 15,000 cycles has been carefully explored, including operating tests in the range 3,000 to 10,000 cycles per second, and it has been found that there is a rather broad optimum in this range. Various theories and tests of the way in which noise varies with frequency are available. Some of these lead to the conclusion that noise varies inversely as the frequency, and others that it varies inversely as the square of the frequency. Careful tests through the frequency range up to 15,000 cycles were made at four locations along a railroad where severe noise came from parallel power lines and adjoining large industrial plants. The best expression of the results is that they varied, on the average, at a rate between these two limits, about inversely as the 1.7 power of the frequency, but the fluctuations from this average were large. To get these data the noise in a fixed band width in cycles was compared to the equivalent single frequency track current. The result of these considerations and tests was that at first 7,000 cycles was chosen as the carrier frequency, but after studies and tests in which the Bell Laboratories co-operated this was later changed to 5,700 cycles at their suggestion. This choice is at the same time a good compromise between attenuation and noise level. In these bands there is no interference in either direction between our circuits and signal circuits or radio.

Single Side-Band Transmission

The decision to transmit only one side band with suppressed carrier was based on the following considerations:

1. The efficiency of the transmitter power unit is greater. The expression for the power transmitted with a carrier modulated to produce two side bands is $(K^2/4)+1+(K^2/4)$, where K is the modulation factor and 1 is the carrier power. This leads to a total power of 1.5 with only 0.5 in the two side bands, with 100 per cent modulation or $K=1$. By transmitting one side band only, all of the transmitted power may be put in it, and the efficiency of intelligence transmitted is improved by a factor of three for 100 per cent modulation; and for K anything less, the ratio of improvement is still greater.

2. Since noise picked up is proportional to the received band width, a large improvement is possible by having the receiver accept only the frequencies represented by a single side band. In the case of a transmitted voice band, 500 to 2,300 cycles, the received width need be only 1,800 cycles instead of the 4,600 cycles necessary when receiving carrier and both side bands. The factor of improvement here is about $2^1/_2$ to 1.

3. It was realized that when a signal is sent over a system in which the attenuation varies rapidly, as it does on a moving train, a much more uniform received signal results with single side-band transmission. When both the carrier and the side bands are transmitted, the signals are obtained by the functioning of the demodulator so that the output is dependent on the product of the amplitudes of carrier and side band. If both carrier and side band change by a ratio of 10 to 1, the received signal could change 100 to 1. When the side band only is transmitted and the carrier is resupplied at the receiver, the side band is the only term subject to variable attenuation, or, in the case just cited, the variation would be only 10 to 1 instead of 100 to 1.

Voice-Frequency Band Used

Intelligibility has been determined by so-called articulation tests similar to those used extensively by the Bell Laboratories. The per cent of meaningless three-letter syllables correctly received by a number of listeners is called per cent articulation. It is much more difficult correctly to interpret unrelated syllables than sentences; for instance, 70 per cent articulation gives practically 100 per cent intelligibility, and 60 per cent articulation gives 99 per cent sentence intelligibility. A study of the Bell Laboratory data indicated that we could exclude the frequencies below 500 cycles and those above 2,300 cycles without serious loss in sentence intelligibility. As a result of many tests made under severe noise conditions, this band 1,800 cycles wide, that is, from about 500 cycles to 2,300 cycles, was chosen as being best for our purposes, including naturalness of reproduction, which is the chief gain from including frequencies below 800 cycles.

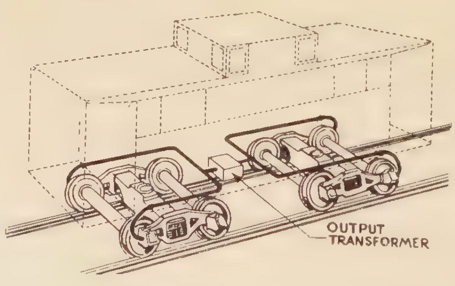


Figure 8. Phantom view showing direct-coupling output circuit on caboose

Pre-emphasis

Another expedient used here, and sometimes elsewhere when it is desired to transmit the maximum possible intelligence with limited sending power, is to set the transmission and reception characteristics of the apparatus in accordance with the average energy of the voice at various frequencies. At 500 cycles the average amount of energy in the human voice per cycle band width is several times greater than at 1,500 cycles; so if the transmitter is fully modulated by voice frequencies around 500 cycles, it will be far from fully modulated by those in the neighborhood of 1,500 cycles. To make the frequency characteristic of the transmitter the inverse of this voice-frequency energy distribution, we can make the response about 40 per cent of full sensitivity at 500 cycles, increase it uniformly to 100 per cent at about 1,400 cycles, and maintain full sensitivity from 1,400 to 2,300 cycles. In this way all components of voice frequencies modulate the transmitter to about the same extent. At the receiving end the frequency distribution is corrected by having the amplifier gain about 2.5 times as much for frequencies representing 500 cycles as for those representing the band from 1,400 to 2,300 cycles, with a uniform drop from 500 to 1,400 cycles, and in this way the received speech is restored to its normal energy distribution. Without a change in the frequency characteristic of the transmitter, the receiver would have to operate at all frequencies at the same sensitivity so that the reduction in sensitivity of the receiver for higher modulation frequencies represents a reduction in the noise level picked up in this band without any loss in reception of the signal desired. With the values chosen this represents about 2 to 1 improvement in the signal noise ratio of the received signal.

Coupling to Track for Transmission

Originally the coupling between the transmitter and the rails was made by means of large iron-core coils carried six or eight inches above the rails, but it was found possible to transfer only about one half of one per cent of the power in the coils into power in the track. The so-called direct coupling circuit of Figure 8 is 15 or 20 times more efficient. Figure 9 gives typical measured impedance values for such a circuit applied to a caboose. The large copper pipe used for the conductor has a resistance of 0.01 ohm, whereas the rail resistance is 0.04 ohm, so in this respect the circuit is fairly ef-

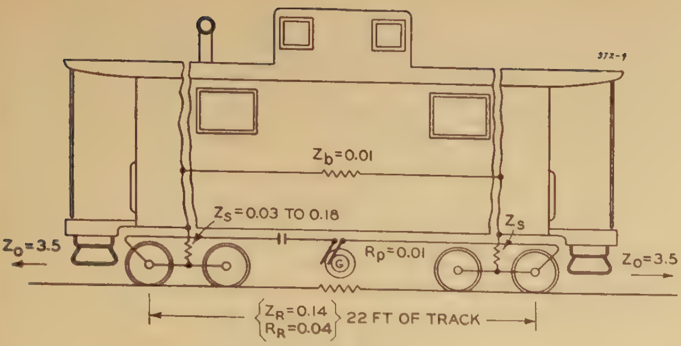


Figure 9. Impedance values for output circuit on caboose

ficient in that about 80 per cent of the power in the output circuit goes into the track itself. However, the coupling between this circuit and the rest of the track is very inefficient. The impedance of the rails from contact to contact on the two ends of the vehicle is 0.14 ohm, and the impedance of the track looking in either direction is about 3.5 ohms for three-ohm ballast. Thus, this circuit, considered as a source of power into the rails beyond the vehicle, is badly mismatched. If we consider the section of the rail under the caboose as the impedance of the source 0.14 ohm, it is feeding into a load of seven ohms, or the impedance ratio is about 50 to 1. Considering as 100 per cent the efficiency of transmission for maximum power transfer when the impedance of the source and the load are equal, then the approximate efficiency for a mismatch is given by the expression $4n/(n+1)^2$ and this, if $n = 50$, gives 7.7 per cent. This expression neglects the phase angles of the impedances, but an accurate calculation including these gives about the same result—8 per cent.[1] It is thus seen that because of the mismatch of impedances we are losing about 92 per cent of the power which we would like to put into the track beyond the vehicle. In case of sending from a wayside station, it is possible to make this factor better than 100 per cent, for we can connect directly between rails and ground, balance out reactance, and, for three-ohm ballast resistance, make our source resistance about 1.30 ohms, which is the real part of the characteristic impedance to ground for the track extending in the two directions in parallel.

Inspection of the output circuit diagram, Figure 9, reveals that there is a shunt path through the vehicle tending to short out the rail which, in the average case shown, might have a value of about 0.2 ohm. This, in parallel with the rail impedance of 0.14 ohm, is not extremely serious and in the case of a light vehicle, such as a caboose, gives fairly satisfactory communication without insulating the trucks. However, the shunt impedance is quite variable, and in the case of the lowest values shown, which are measured values on a steel caboose, an impedance of only 0.07 ohm would be put in shunt with 0.14 ohm in the rail, and a severe lowering of efficiency would result. This situation is very much worse in the case of a locomotive or tender because of the very much greater weight involved. One theory of contact resistance[2] indicates that it varies inversely as the $^1/_3$ power of the ˙orce on the contacts. Experiments indicate the exponent may vary from $^1/_3$ to 1. A caboose weighs about 20 tons, and a tender may weigh 200 tons, so by this theory, taking the contact resistance variation inversely as the square root of the weight, the contact resistances through the body of the tender would be less than one third as great as through the body of the caboose, and these low values in shunt would cause serious impairment of transmission. In the trains now equipped and operating, trucks in both the caboose and locomotive are insulated from the body of the vehicle. The pilot truck of the locomotive is usually chosen for insulation, and the front coupler is also insulated to prevent undesirable shunt paths.

Effect of Transmitting Loop Position

At first it was thought that by having the copper pipe of the loop high, a better circuit would result, principally because by getting the pipe further away from the rail the self-inductance of the rail would be increased, and a given current would give considerably more rail voltage effective for transmission. This is probably true for a wooden caboose, but it has been determined experimentally that it is certainly not true for a steel vehicle which covers almost all the newer design present-day cabooses and tenders. The characteristics of a high loop over the top of the vehicle were compared with a low loop in which the copper pipe is placed just under the floor of the vehicle, and the somewhat surprising result was found that the low loop is more efficient. There are two reasons for this:

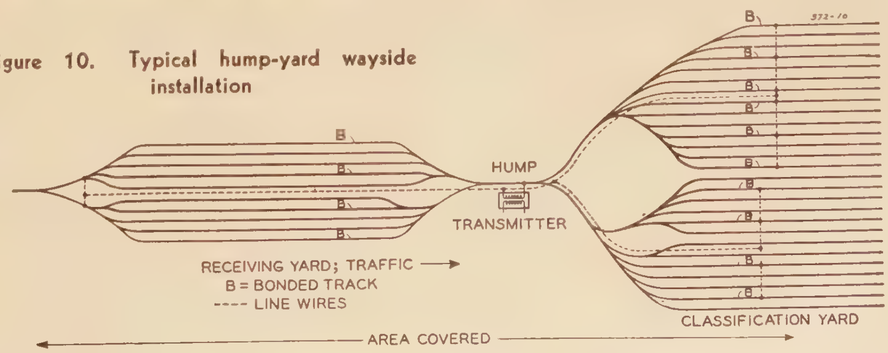
1. The presence of the steel body practically eliminates any gain from added height, so that the actual effective height of the high loop is almost the same as that of the low loop.
2. Considerably more copper pipe is needed for the high loop, thus increasing copper loss.

The effective height of the high loop was determined by five separate measurements made in different ways. These all led to the same conclusion that the effective height of the high loop was almost exactly the same as the clear air space between it and the rails. In other words, a loop about 14 feet high functioned just the same as one about four feet high, which represents the amount of clear space from ground to the floor of the vehicle, plus the distance from the caboose roof to the high loop.

Relation of Track and Parallel Line Wires

In the system of communication under discussion, the signal is fed into the track and is picked up from the track, usually with assistance in the transmission by mutual induction to and from the paralleling line wires which are nearly always to be found along the right of way. Measurement and calculation show that the induction into the line wire is principally from the track and not from our sending loop. This is not surprising when one takes into account that the length of exposure between the track and the line wires is much greater than that between the sending loop and line wire. It can be demon-

Figure 10. Typical hump-yard wayside installation



strated that the voltage induced in a line wire from a voltage introduced in a track is independent of the ballast resistance of the track, and hence of wet or dry seasons. This can be seen qualitatively by considering that if the ballast resistance is low, larger track currents flow, but they attenuate more rapidly. Analysis shows that the two effects just balance one another. In order to receive the strongest signal, however, experiment shows that it is necessary to orient the coils for maximum coupling with the rails rather than with the line wires.

When sent from a wayside station, the signal is ordinarily applied between a line wire and a track as a ground connection, rather than between tracks, although the latter is done sometimes. With such use of a line wire, conversation between a tower and a moving train 100 miles distant is practical.

Yard Communication

An important application of train communication is found in yards where it is desired to direct the work of several locomotives from one office. Some yards use one-way equipment, office to engine only, while others use two-way equipment which permits the engineman to talk to the office. A typical railroad hump yard is shown in Figure 10. Here communication is desired between the hump and locomotives working any place in the receiving yard and past the entering switches of the classification yard. In order to furnish this communication, line wires are provided as shown dotted in the figure. These line wires may be on pole lines if these are present in suitable location, or clearances permit building new ones. It is generally best not to have these wires immediately adjacent to open-wire telephone pairs unless special transpositions are used, but pole lines carrying power, telegraph, or signal wires may be used. Often a buried single conductor Parkway cable with nonmetallic sheath is used. Lines are supplied so that no point in the area to be covered is more than ten tracks away from a line wire for one-way communication, or more than seven tracks away for two-way communication.

One-way yard communication depends on rail currents which flow because of conductive connection to or induction from the line wire. Hence, to provide positive rail paths, one rail of each third track is bonded so that a locomotive will always have rail current flowing in its own or in the immediately adjacent track, which is near enough for satisfactory inductive pick-up of the signal. For two-way communication, one rail of each track is bonded to provide positive rail paths for the locomotive sending currents. When trains operate on well maintained main-line track, it is not necessary to bond, but bad electrical connections in rail joints are common in yard tracks.

The sending voltage at a yard office or wayside station is connected between the line wire and ground. Any bonded track of considerable length may be used for a ground. At the limits of the area covered, the line wires are grounded to the bonded tracks. Direct connections between line wires and tracks are not necessary, since mutual induction between line and track will supply communication, but the connection line to rail is usually the most convenient way to terminate both line and bonded tracks at the coverage limits.

Approximate calculations can be made of the communication currents in such railroad yards. A primary factor is the mutual induction between a line wire and a track, and this may be determined from published charts.[3]

There are several other effects to be taken into account, one of which is the loss occurring when the signal is fed into one wire in a group of wires running parallel to one another. Opposing currents will flow in the other paralleling conductors, which will tend to reduce the effect of the wire employed. Two-thirds as much current as flows in the wire which carries the signal current may be induced in an immediately adjacent wire, and a rough calculation indicates that the net induction from the transmitting current in a single wire which is a member of an open wire lead can be estimated by dividing the calculated value for a single wire alone by three times the square root of the number of additional paralleling wires. Likewise, the currents induced into any one track are reduced by mutual induction from the currents flowing in adjoining tracks. A study of this effect indicates that current calculated in any one track considered alone must be divided by the square root of the number of tracks between it and the line to approximate the actual current.

In considering transmission in the opposite direction, the effect of interfering tracks is the same; that is, they interfere with transmission from a track to a line in about the same way. However, multiple wires on a pole line are an aid in this case because the currents in each are approximately inphase and they add in effect on a receiving means able to pick up from all the wires equally well. The results obtained with these methods of calculation agree with the results of some field measurements within ±30 per cent.

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Thermal Rating of Overhead Line Wire

MYRON ZUCKER
MEMBER AIEE

THE major problems in rating overhead wires according to thermal limits are

1. How materials lose their strength.
2. How hot wires become when various currents are passed through them.

Much scattered work has been done on these subjects in the last 20 years, but results have been either inconclusive or, in many cases, contradictory. This discussion brings together data from many sources, adds certain data, and outlines the general problem.

Omitting the obvious discussion of efficiency and voltage drop, let us say that the rating of overhead wires must be such that there shall be no appreciable weakening of the conductor material and clearances at the higher temperatures of operation must be ample. To solve this general problem, we must obtain data on the following specific phase:

I. Permissible Temperatures

(*a*). To retain the strength of wire. This depends on time of heating, impurities in the wire, and to a less degree on amount of cold working prior to heating.

(*b*). To protect wire covering from undue deterioration.

(*c*). To preserve reasonable clearances.

II. Temperature That the Wire Will Attain

(*a*). Basic thermal relations expressed as temperature rise versus current, wire size and material, wind at various directions, and sunshine.

(*b*). Weather conditions under which heating will occur, finding reasonable values of wind, temperature, and rain combinations under normal and emergency operation.

Importance of Frequency and Duration of Overload

The total expected duration of heavy loads affects permissible temperatures, acceptable minimum ground clearances, and probable adverse weather combinations. Therefore, any thermal limits must be made with reference to specified frequency and duration of overload.

I. Permissible Temperatures

I*a*. Maximum Temperature That Will not Anneal Wire

We will confine our metallurgical study to copper. The cold-worked copper used in overhead lines has its crystals so arranged that overheating will start new crystal growth that weakens the wire. Some X-ray diffraction tests (of which details will follow) indicate that the beginning of recrystallization coincides with a loss of five per cent of the tensile strength of the wire. We therefore adopt this as the allowable loss of strength by annealing. The problem is then to express temperature and time that will produce five per cent loss of tensile strength.

The solution cannot be found directly in the literature, because few of the 37 authorities[1–37] consulted give results in directly usable form. However, by correlating some curves (such as Figure 1) and many isolated points, the field can be roughly covered. Figure 1 is a sample of tests in which many pieces of one wire were heated for various times and then broken in tension after cooling. From these curves, the time-temperature combinations for five per cent decrease in strength can be found. The curves presented by different authorities show considerable variation, but results can be reconciled reasonably well by considering the chemical impurities. Fortunately, in this type of copper only oxygen and silver remain in important quantities.

Figure 2 shows the permissible temperatures for one hour. Some of the points are from curves such as Figure 1, but most are from isolated test runs of $^1/_2$ to 2 hours. The latter were corrected to one hour by comparison with other investigators' strength-time curves. Some points are from hardness or elongation tests, corrected to equivalent breaking-strength results from interlocking reports. (A loss of one per cent in Vickers, Brinell, and scleroscope hardness corresponds to about five per cent loss of tensile strength, but Rockwell tests lagged about 50 degrees centigrade in showing the change.)

The trends of the points in Figure 2 are indicated by the labeled areas. From these we see that permissible temperatures increase with silver content, with the most minute parts of silver having greatest effect. We also see that oxygen is only important when below 0.02 per cent.

Typical compositions of wire copper are indicated, but the range of individual coppers is great. "Lake" copper, common before 1924 but since then used only on mile-long seamless draws, varies naturally as mined. The amount of silver in electrolytic copper varies with the value of silver. Oxygen-free copper is not commonly used for conductors but shows what can be accomplished by fine chemical controls.

We now turn to curves of temperature versus time (Figure 3). Considering the wide range of conditions represented by these authors, the agreement in trends is good. The shape of these curves, plus some isolated tests, were used therefore to draw a chart for four values of silver (Figure 4), whose intercepts at one hour were selected from Figure 2. From these curves, if silver is known, temperature may be estimated for any time of heating. If silver is unknown, tests may be run at 200 degrees centigrade for periods varying from one-half hour to a day (that is, data obtained as in Figure 1), and time at which the wire loses five per cent of its strength spotted on Figure 4. The nearest curve can then be taken to represent the action of this material. Such tests substitute for chemical analyses.

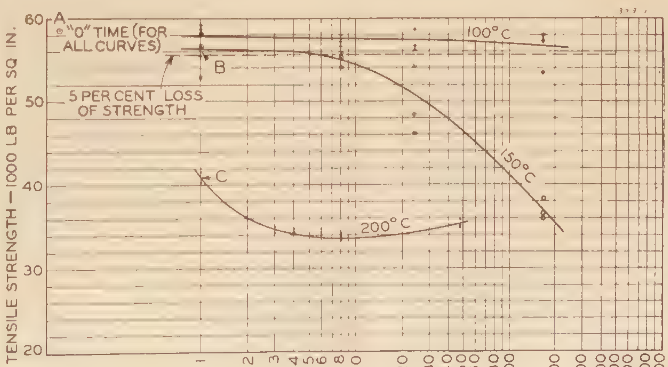

Figure 1. Cold breaking strength in tension versus time of application of several temperatures

For number 6 strands from number 000 copper wire after some years service

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Myron Zucker, on leave from The Detroit Edison Company, is with the Jam Handy Organization, Detroit, Mich.

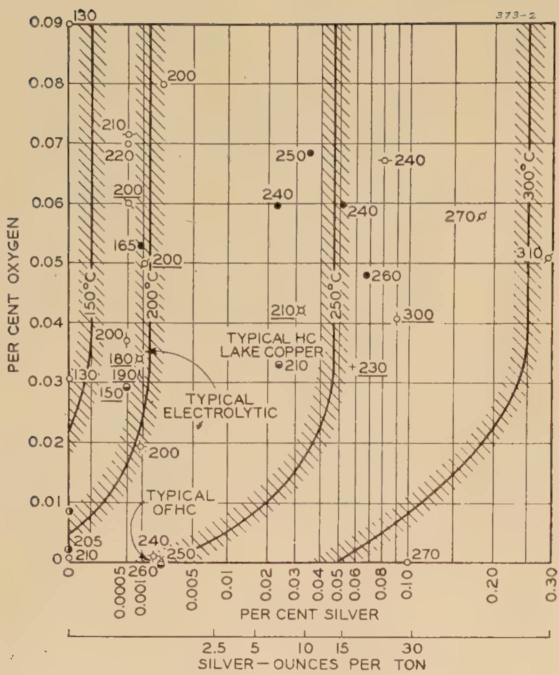


Figure 2. Influence of oxygen and silver on temperatures that will cause five per cent decrease in copper strength if applied one hour

Each symbol represents a different authority

Underlined values are estimated

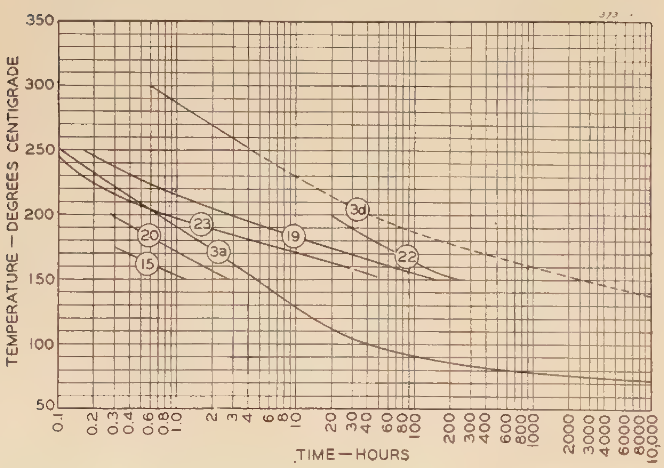


Figure 3. Temperature to begin weakening of copper wire held continuously in oven under no stress

Beginning of weakening shown by one per cent decrease in hardness or five per cent decrease in tensilestrength(cold)

	Copper			
Authority	Initial Strength	Silver (Per Cent)	Oxygen (Per Cent)	Test Method
3*a*..	57–59	..0.001	...0.009	...Vickers, tensile
3*d*..	50±	..0.0017	...0.001	...Vickers numbers
15..	36	.. Not stated		...Tensile strength
19..	51, 55, 57.	.0.0005	...0.07	...Scleroscope
20..	59	.. Not stated		...Tensile strength
22..	70	..0.035–0.069	...0.05 0.065	...Tensile strength
23..	42–62	..Not stated	0.066	...Tensile strength

Metallurgists will note the absence of history of the metal prior to cold-working, and degree of cold working, from this graph. The effects are relatively minor for the strengths, times, and temperatures involved in this problem.

The results are admittedly crude. If the problem continues to be important, we might suggest that outdoor spans be set up, heated as desired, and tested regularly by X-ray diffraction until recrystallization begins. This method would not only avoid the duplication of samples that is necessary in destructive test methods to average out individual variations in material, but permits study of the effect of tension and vibration and also of intermittent heating. The latter three points have been completely ignored so far, and may be conceivably of major importance.

The sensitiveness of the X-ray test is shown by Figure 5. These patterns were made by D. McCutcheon of the Ford Motor Company, at the author's instance. Figure 5*A* shows an unannealed specimen; Figure 5*B* shows a piece heated for an hour at 150 degrees centigrade; and Figure 5*C* shows a piece heated for an hour at 200 degrees centigrade. The black spots in Figure 5*C* are from recrystallization. The specimens when broken in tension (points are shown by *A*, *B*, and *C* on Figure 1) showed 58.3, 56.5, and 40.5 thousand pounds per square inch strengths.

Figure 5*D* is from another strand in the same wire, subjected to the same treatment as Figure 5*C*. Only a moderate number of new crystals appear, and the strength was about 50 thousand pounds per square inch. This strand had more silver than its mates.

To round out the subject of wire strength, we may add that there is no evidence that nicks or burns too small to call for replacement under normal good practice impose any lower limit; tests have shown that modern joints are equal to their proper wires in carrying capacity.

I*b*. Wire Covering

The dripping of compound and loss of dielectric may impose limits on the heating of covered wires.[39] No results are yet available that include the time element. Recent tests on *URC* covering indicate that it may safely be taken to 100 degrees centigrade for ten hours.

I*c*. Clearances

To operate lines at high temperatures, the increased sag should be computed and the line surveyed to see whether that much loss of clearance can be spared, considering the expected frequency of heavy electric loading. As an example, sags after heavy ice-and-wind loading of a number 000 copper line with 600–800 foot spans is about 2.8 feet more at 210 degrees Fahrenheit than at 120 degrees Fahrenheit and 4.5 feet more at 270 degrees than at 120 degrees.

II. Temperature That the Wire Will Attain

II*a*. Wire Heating

The studies of Schurig,[40] Luke,[41] and others,[42–52] have given a reasonably good basis for predicting temperature rise of wires under a limited range of temperature rise and wind speeds. Further tests over wider ranges, and including various wind dirctions, were made by the Detroit Edison Company last spring, when decisions were required on the rating of certain transmission lines. Figure 6 shows a typical set of data. The curves are self-explanatory.

According to these later tests, when there is no wind the temperature rise depends on the 1.9 power of current (running from 1.8 to 2.0 for different wire sizes). As the temperature rose, the local air current induced by heating increased until at about 150 degrees centigrade rise the vertical air movement

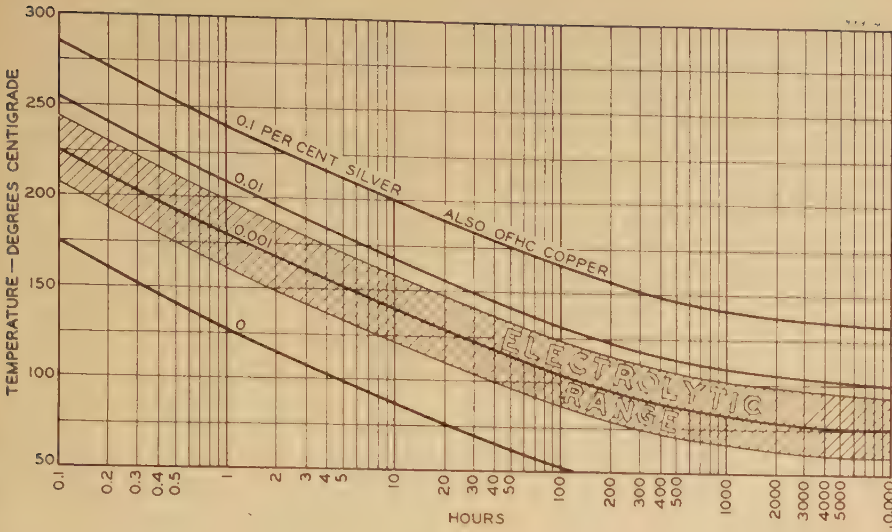


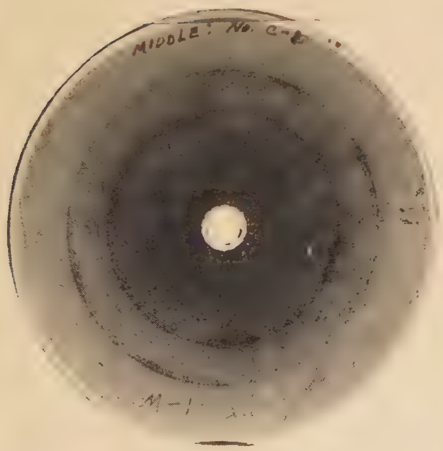
Figure 4. Temperature versus time to reduce strength of copper wire by five per cent

For various silver contents when oxygen is 0.02 per cent or more. The curve for 0.1 per cent silver also may be used for typical oxygen-free high-conductivity copper

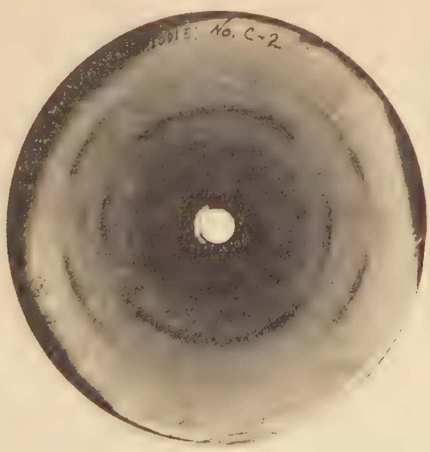
was one half mile per hour. This circulation was more effective, per unit speed, in carrying away heat than equal real wind speeds. This accounts for the difference in slope between the no wind and wind curves.

A. No heating

B. After one hour at 150 degrees centigrade



C. After one hour at 200 degrees centigrade



D. High silver-copper after one hour at 200 degrees centigrade

Figure 5. X-ray diffraction patterns for copper wire

Wind, even at low speeds, was found to decrease temperatures materially. The temperature varies as the 2.3 power of current. The direction of wind is now of prime importance; when parallel to the wire, a 2.4 mile per hour wind permits temperature to rise 205 degrees centigrade compared with 180 degrees for wind at right angles to the wire.

From data available at the time, the nomograms of Figures 7 and 8 were drawn. Some inconsistencies are being checked by further tests, but the charts are believed reasonably accurate.

It will be noted that sunshine has had no place on these charts. At the elevated temperatures, the influence is minute. More important, we should note that wind is a more critical factor than is a variation of temperature in the range of ambients ordinarily encountered. For instance, the following combinations will heat number 0000 bare copper wire to 100 degrees centigrade:

Ambient Temperature (Deg C)	Wind Velocity (Miles Per Hour) 0	1	2	5	
10	400	540	570	650	amperes
30	350	480	510	600	

The temperatures listed, change the current about 12 per cent, whereas one mile per hour of wind increases the current about one third over the calm.

IIb. Weather

Weather is really a corollary to part IIa, but it takes so much effort as to deserve separate treatment. Because of the present shortage of copper, we cannot

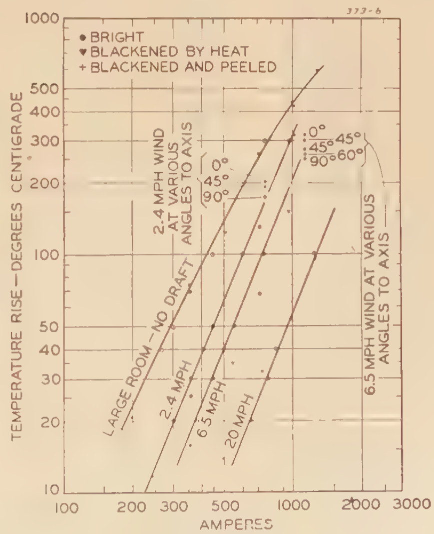


Figure 6. Temperature rise versus current

In number 000 bare stranded copper wire for various wind and surface conditions

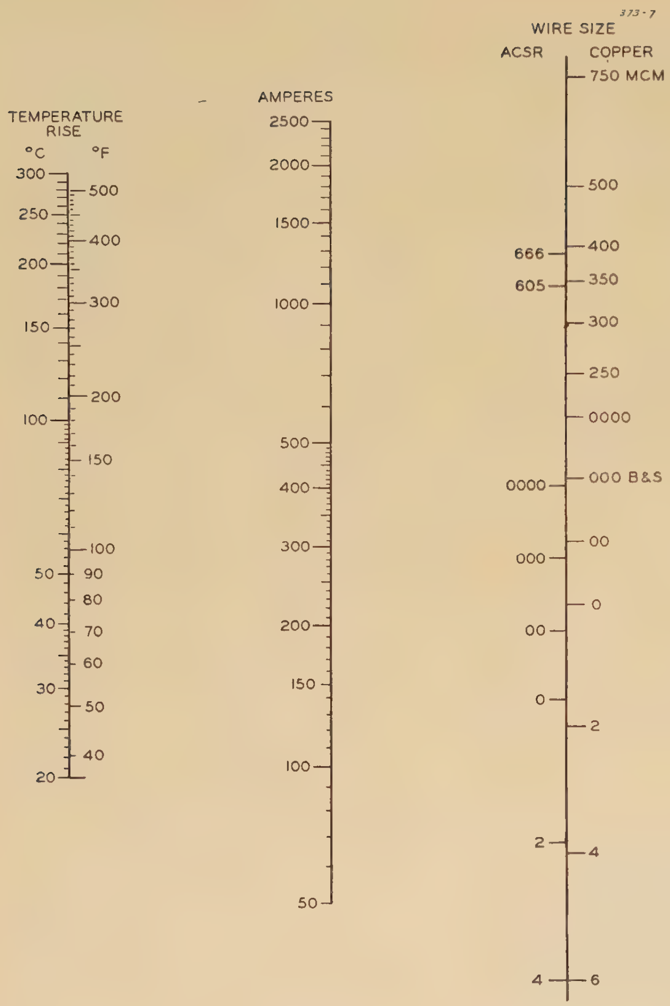


Figure 7. Temperature rise of bare copper wire and steel-reinforced aluminum cable

When there is no wind

Increase current five per cent for blackened conductors. Decrease current five per cent for bright conductor. Increase current 10 per cent for steel-reinforced aluminum cable conductors

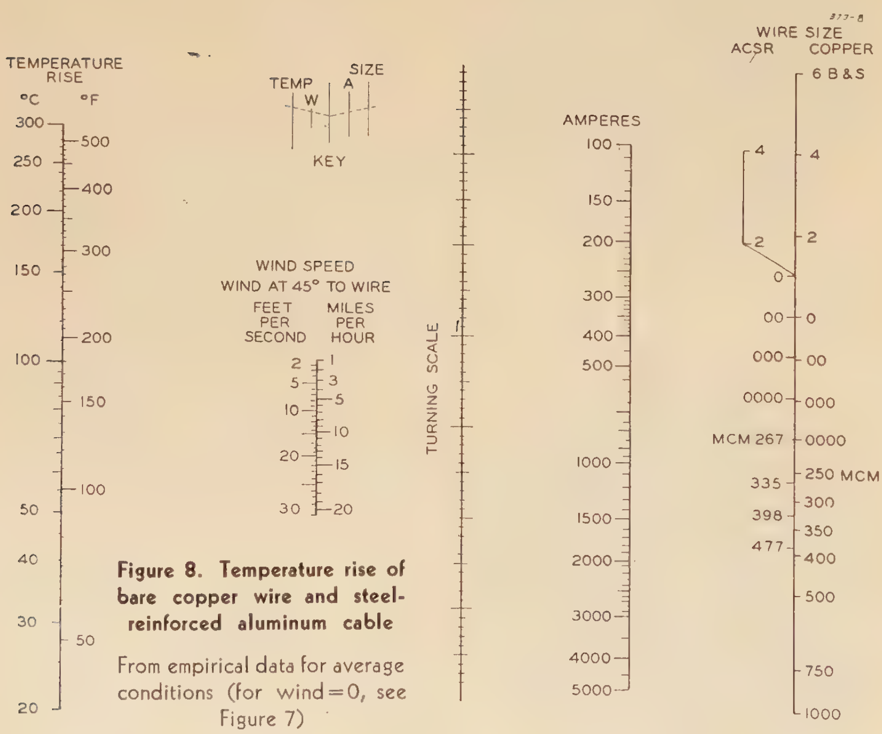


Figure 8. Temperature rise of bare copper wire and steel-reinforced aluminum cable

From empirical data for average conditions (for wind = 0, see Figure 7)

work on the "safe side" assumptions of most adverse weather conditions.

The procedure in finding reasonable combinations of wind and temperature to use in Figures 7 and 8 depends on the refinement desired, and on the load cycles or emergencies being studied. Figures 9, 10, and 11 show one form in which data may be presented for normal rating of a circuit that has continuous daytime load. A preliminary study of the weather observatory records showed that in summer, besides temperatures being highest, winds were lowest. Therefore, records were compiled for summer only. Further scrutiny showed that the coincidence of rain and low winds was so small that rain can be neglected.

Figure 9 shows the distribution of wind and temperature for each month from March through September, 1940 and 1941, during the normal peak-load hours of 8 a.m. through 5 p.m.

These data are condensed in Figure 10, with frequency plotted against wind speed for winds up to five miles per hour. The meagerness of high-temperature low-wind combinations is evident.

Finally, Figure 11, showing frequency of various winds, regardless of temperature, gives a straightforward curve for choosing wind speed after a permissible frequency has been selected. Data are for July and August (which Figure 9 shows to have the worst combinations) for five years, and curves are plotted for both the part of the day when the electrical load is heavy and for the off-peak period. Percentage of calms is 1.5 for on-peak hours, or on the average 6.5 per month.

On this basis, considering the number of hours the heavy load is expected to endure, the permissible operating temperature for the wire may be selected from Figure 4 and corresponding current from Figure 7.

Frequency curves are more important under emergencies, when they give data for comparing the coincidence of heavy loads with various weather conditions according to the theories of probability.

Figure 12 shows a special case: average wind and temperature following the passage of the intense phase of lightning storms, in an area where transmission line outages were almost all caused by lightning. There are no cases in which average wind was less than six miles per hour, and temperatures are generally 80 degrees or cooler. Incidentally, on the average there were four hours of rain in the eight-hour period. With such data, wire rating may be specified for these conditions. This should be checked against the one-hour values, and allowance be made for the proper proportions of outages from other causes.

This type of investigation requires much labor. It is often warranted, however, by the increased rating that can be assigned to a line with reasonable assurance. Incomplete data may be misleading—but still not so much as values chosen by common-sense estimates.

Summary of Conditions in References on Annealing of Copper

Reference Number	Author	Initial Conditions: Strength (Thousand Pounds Per Square Inch)	Initial Conditions: Per Cent Cold Reduction	Initial Conditions: Strand or Gauge	Type Copper: Per Cent Silver	Type Copper: Per Cent Oxygen	Type Copper: Commercial	Test Conditions: Tension While Heated (Thousand Pounds Per Square Inch)	Test Conditions: Test Method	Test Conditions: Variables: Time (Hours)	Test Conditions: Variables: Temperature (Degrees Centigrade)
1	Wacker	62	71	6	*NS*	*NS*		0	*T*	*x*	100–200
2	Unknown	52	71		*NS*	*NS*		0	*T*, *E*	*x*	250 and 300
3	Voce	60–57 59–57 59–58		14–6 14–6 14–6	0.0017 0.001 0.00006	0.001 0.019 0.030	*OFHC* *E* *EC*	0	*T*	*x*	80
		50±	50	0.56 inch	0.0017 0.001 0	0.001 0.019 0.030	*OFHC* *E* *EC*	0	*V*	1 and 24 *x*	200 and 600 and 250
4	Beedle	65		6	*NS*	*NS*		10	*T*	48	100–150
5	Zickrick			34	*NS*	*NS*				0.05	225–325
6	Bradley	73			*NS*	*NS*					25–500
7	Talbot	*NS*	*NS*	*NS*	*NS*	*NS*	*E*	0	*J*		
8	Price	48	25	*NS*	*NS*	Low	*E*	0	*TH**		150–1,000
9	Jares	57		*NS*	*NS*	*NS*	*E*	0	*T*	*x*	180
10	Bassett					See reference 24					
11	Bassett	*NS*	*NS*	*NS*	*NS*	*NS*	*E*				
12	Taylor	Soft		6	*NS*	*NS*	*NS*	5	*T*	*x*	
13	Parker	30	0		0.001 0.002 0.054	0.035 0 0	*E* *OFHC* *OFHC* and silver	High	*T*	*x*	200 200 200 and 250
14	Fuller	*NS* *NS*	*NS* *NS*	*NS* *NS*	*NS* 0.03	*NS* *NS*	*E* *L*	High	*T* *T*	20 140	165 300
15	Alkins	63	93	0.26 millimeter	*NS*	*NS*		*NS*		*x*	150–200
16	Pratt	58 56 58	62 62 62	*NS* *NS* *NS*	*NS* *NS* *NS*	*NS* *NS* *NS*	*E* *L* *OFHC*		*T*, *E*	1	*x*
17	Zeerleder	*NS*	*NS*	*NS*	*NS*	*NS*	*NS*	*NS*	*NS*	1,000	80
18	H. Moore	60 58		14–19	*NS*	*NS*		0	*T*	*x*	100
19	Caesar	57	69	Strips	0.0005	0.07	*E*	0	*S(T)*	1/2	100–500
		NS	67	Strips	0.1	0	Pure+silver				
		NS	67	Strips	0	0.09	Pure, oxidized				
		53	67	Strips		Pure copper (special)		0	*S*	1/2	100–500
		NS	67	Strips	0	0.03	Pure, slightly oxidized				
		NS	67	Strips	0.002	0.08	Pure, oxidized plus silver				
		57–51	69–51	Strips	0.0005	0.07	*E*	0	*S*	*x*	150–300
20	Offer	62 before aging 59 at test		6		Present	*E*	0	*T*	*x*	100–200
21	Wyman	64	58, 85	8	0.024–0.058	0	*OFHC*+ silver	34	*E*	*x*	105
		64	58	8	0.024	0	*OFHC*+ silver	26–34	*E*	*x*	105
		62	50	8	*NS*	*NS*	*E*	26–34	*E*	*x*	105
		62	50	8	*NS*	*NS*	*E*	24	*E*	*x*	150
			20 37 60	Sheets	0.001–0.132	0.05±	*L*	0	*R*	1/2	200–400
22	Kinney	70–75	99.8	24	0.001–0.132	0.05±	*L*	0	*T*, *E*	0.033	275, 300, 325
		70–73	97.5	16	0.001–0.32	0.05±	*L*	0	*T*	*x*	150 and 200
23	Pilling	42–62	23–84		*NS*	0.066	*E*	0	*T*	*x*	100–900
24	Bassett	58	10	Nos. 0.05-inch sheet	0.0005+ 0.0868,	*NS*	*E*, *L*	0	*T*, *E*	*NS*	0–1,000
25	Rolle	59 59–63	62 62–91	0.5-inch tube	0.0022 0.002	0 0	*OFHC* *OFHC*	0 0	*T*, *E* *T*, *R*	1 1	190–700 250–400
26	Webster	56 59	62–90 62–90	6–0 6–0	*NS* 0.0216†	0.0285 0.033	*E* *L*	0	*T*, *E*	1	180–800
		57			0.002	0.0	*OFHC*	0			
27	Angus	*NS*	33–78	0.28 and 0.31 inch	*NS*	*NS*	*E*	0	*B*	1	100–600
28	Grard	56	57	2 millimeters	*NS*	*NS*	*E*	0	*T*	0.5	100–300
29	Bardwell	66		12	*NS*	0.026–0.07	*E*	0	*T*	0.33	100–400
30	Mathewson	51	50	0.065 inch	0.0005	0.071	*E*	0	*T*, *E*	0.67	100–1,000
31	Zickrick	62–69			*NS*	0–0.028		0	*T*	0.1	200–400
32	Archbutt										
33	J. L. Gregg										
34	Lorig										
35	Widman		98		0 0.24						
36	Hudson		28 50		0.05 0.08	*NS* *NS*	Pure+ Silver	0	*B*	2	300–500 250–500
37	Johnson	28–59	0–98 60	Strip	<0.01 *NS*	0.05 0.089	*E*	0 0	*T*, *E*, *S* *T*, *E*	1 0.5	200–800 200–470

Symbols:
In column "Commercial Type"
P—"Pure"
E—Electrolytic
L—Lake
C—Chilean

OFHC—Oxygen-free high-conductivity
"*NS*" = Not stated
In column "Test Method"
J—Judgment
T—Tension test

E—Elongation
R—Rockwell hardness
B—Brinell hardness
V—Vickers
S—Scleroscope
H—Hardness

In column "Time" an "*x*" indicates that a time-temperature curve is given
*Apparently tested while hot. Strength lowered at lowest temperature tried.
† High in arsenic and antimony.

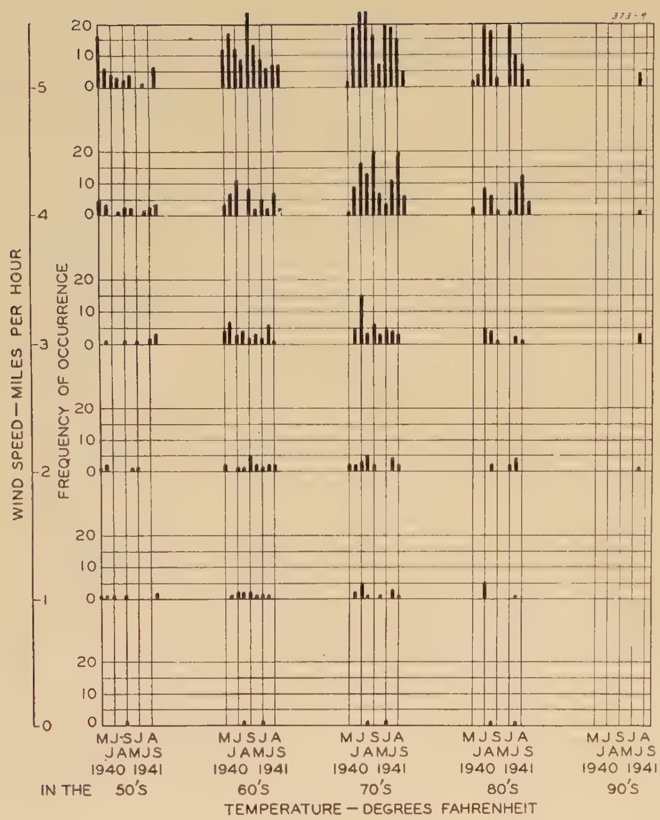


Figure 9. Wind and temperature

Number of hours occurrence of various values at centrally located observatory

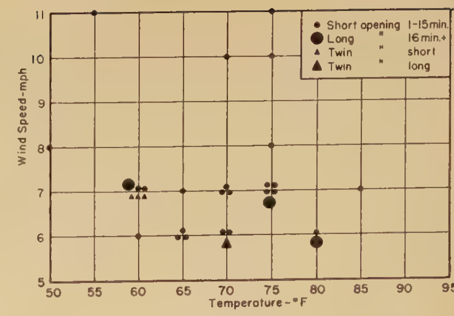


Figure 12. Average wind—temperature

For eight hours following storms that caused 120-kv line openings in 1941. Values at centrally located observatory

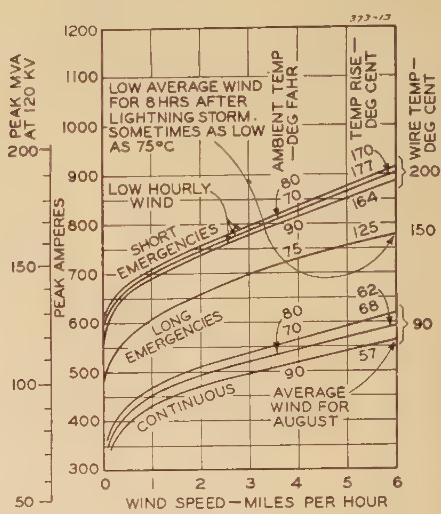


Figure 13. Composite chart for selecting wire rating

Number 000 *HD* copper

III. Combination of Data

When we take into account all the considerations discussed, the situation seems involved. If it is visualized as in Figure 13, the relations become clearer. Curves are plotted showing permissible current against wind speed for number 000 copper containing 0.004 per cent silver. Three conditions result:

1. Continuous load (lowest curve).
2. 20-hour single-circuit outage of a twin-circuit 120-kv line.
3. One-hour outage of the same line.

From Figure 4, the permissible temperatures are found to be 90, 150, and 200 degrees centigrade respectively. Using these temperatures and reasonable ambients, from Figures 7 and 8, we derive values of current for the expected range of wind. On this graph we indicate the probable wind-temperature combinations. Thus for case 1 the record shows an average August wind of about six miles per hour. From Figure 13, we see that about 550–600 amperes can be carried. This is not enough to satisfy the conditions for long-time loading. There may be other lower limits. For instance, the 1.5 per cent of calms in August, plus other hours of low wind, may impose a lower limit, even though the permissible temperature is higher.

If we knew more about the effect of intermittent heating upon annealing, we would be justified in developing a method of integrating the temperature-time relationship in a manner similar to the procedure on transformers.

The other curves and weather data shown on Figure 13 should be considered similarly. Such visualization indicates the range of currents that can be reason-

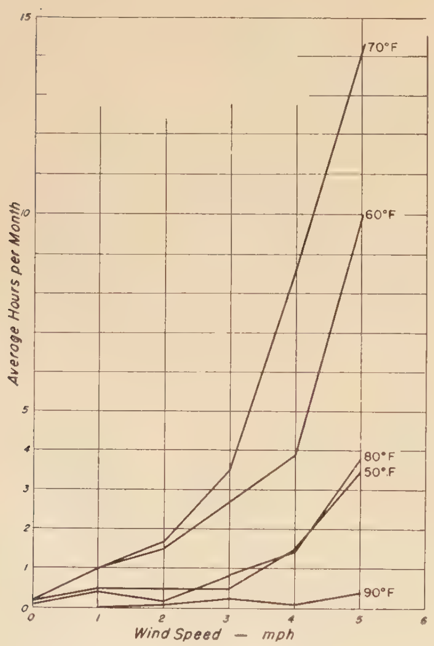


Figure 10. Incidence of low winds at various temperatures

May to September 1940–41, 8 a.m.–5 p.m. Figure shows small number of cases at 90 degrees Fahrenheit all with two miles per hour or more

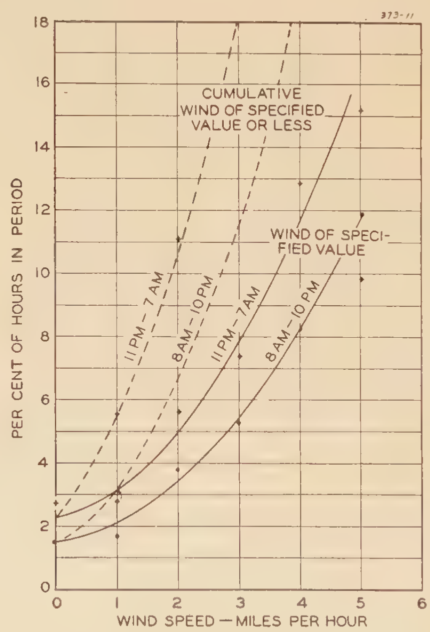


Figure 11. Frequency and cumulative frequency of winds in July and August 1935–40

Values at centrally located observatory

ably used and shows the desirability of adjusting the ratings to each set of conditions encountered in operation.

This report summarizes a great deal of information of electric rating of overhead wires on a thermal basis. It gives a reasonable working basis for most cases but indicates there are many gaps to be filled: in finding how intermittent heating under tension and vibration affects heating, the temperature limits on wire coverings, resolving some discrepancies in temperature-rise data, in computing expectancies of loads and weather combinations, and in developing a simplified method of applying these data in the field.

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* Especially good reports.

Advantages of High-Speed Traction Motors

C. A. ATWELL
MEMBER AIEE

THE present use of high-speed light-weight propulsion motors on various types of traction vehicles is the result of an engineering evolution. An important phase of this development began approximately fifteen years ago, but the greatest progress has been during the past seven years. The motors resulting have definite advantages over their larger and heavier predecessors, and it is the purpose of this paper to point out and evaluate the more important of these advantages.

Historical

Prior to 1927 practically all of the traction motors in use on streetcars and interurban and subway cars were of the axle-hung type, geared to the wheels by a pinion on the armature shaft and a gear on the car axle. During the middle 20's there was a demand for motors of much lighter weight for use on gas-electric and trolley coaches. This demand was met by the design of motors of considerably higher speed and connected to the wheels through propeller shafts and automotive-type axles. These early high-speed motors were somewhat hybrid in construction, utilizing experience obtained from the lightest weight axle-hung streetcar motors and those used on electric battery trucks at that time. It is significant that nearly all of them used commutating poles and all employed anti-friction armature bearings.

In 1928, the Westinghouse double-reduction *W-N* gear-unit drive was developed for streetcars. This required a high-speed motor which had approximately 35 per cent of the weight and double the maximum armature speed of its axle-hung predecessor. Since that time, much design, research, and operating experience has been combined to produce the modern motors that are used today on the various forms of electrically driven city and intercity transit vehicles and some types of mining and railway locomotives. The following discussion enumerates and evaluates the principal advantages of these motors:

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C. A. ATWELL is design engineer in the transportation engineering department of Westinghouse Electric and Manufacturing Company at East Pittsburgh, Pa.

Reduced Size and Weight

The principal feature that permits motor designs of decreased size and weight is the gearing used which allows higher-speed armatures. The maximum gear reduction usable on the axle-hung motors was around 6 to 1, and reductions used were usually around 5 to 1. This was because of an inherent limitation in gear reduction in the tie-up between the center lines of armature and axle, gear-tooth strength, and clearance from gear case to rail. With the motor separated from the gears, a gear ratio is selected which allows the motor armature to rotate at its highest economical speed. The result is that traction-motor gear ratios from 6 to 1 up to 22 to 1 are now used, depending on the application and type of vehicle.

The size and weight of a motor armature are approximately proportional to its torque. Horsepower is proportional to the product of speed and torque. If a given horsepower rating can be obtained from more speed and less torque, the armature is smaller in proportion to the decrease in torque. Another reason for smaller armatures on recent motors is the saving in space effected by modern insulation of the armature coils. The development of thinner and stronger mica and glass tapes and wrappers has made this possible. Any decrease in armature diameter by improvements such as this means that the rotational speed can be increased further, and thus a still smaller armature for the same horsepower can be used. A smaller armature carries with it a reduced field structure, and therefore the whole motor is reduced in size and weight. Figure 1 shows a comparison of motor weights. The comparison is between the latest light traction axle-hung motors *A*, and modern high-speed motors developed since 1935–*B*. The weights given are exclusive of gears or gear housings in both cases.

Figure 2 shows comparatively how the size of traction motors having a one-hour rating of 125 horsepower has varied over a period of years.

One of the advantages of the reduced motor size is the saving in material. The saving of copper is especially important as an aid to the war effort. The copper saving in motors used on 2,100 Presidents Conference Committee streamlined streetcars recently built in this country and Canada is approximately 1,250,000 pounds. This saving is estimated by comparing the weight of copper in the present motors used on these cars with the weight of copper in the most recently designed axle-hung streetcar motors of the same rating.

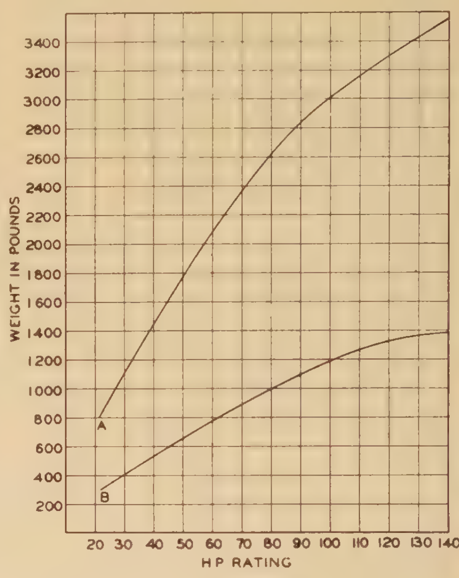

Figure 1. Motor weight comparison

A. Weights of most recent axle-hung streetcar and interurban car motors

B. Weights of modern high-speed traction motors

Another important advantage of decreased motor size and weight is the allowable reduction in truck or chassis size and weight. Modern streetcars, trolley-coach, or Diesel-electric-coach construction would not be possible if the modern light-weight motors were not available.

Increased Ventilation

The higher-speed armature permits a fan design that provides increased ventilation. This causes the continuous rating more nearly to approach the one-hour rating. The slower-speed motors had continuous ampere ratings that were between 60 and 75 per cent of the one-hour rated amperes. The high-speed motors have continuous ratings between 80 and 90 per cent of the one-hour rating.

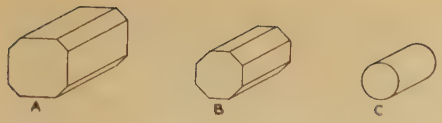

Figure 2. Graphic comparison of traction motor body size

A. Rated 600 volts, 125 horsepower, 275 rpm (developed in 1909)

B. Rated 600 volts, 125 horsepower, 800 rpm (developed in 1915)

C. Rated 300 volts, 125 horsepower, 2,000 rpm (developed in 1939)

Higher Efficiency

The high-speed motor is fundamentally more efficient than a lower-speed motor of the same voltage and rating. This is because fewer conductors of shorter length are required both in the armature and field coils. This reduces the copper I^2R loss which is a major part of the total loss especially at heavy accelerating loads. The iron losses and friction losses of the high-speed motor may become greater at low values of tractive effort, but the small per cent of operation at these low loads makes the reduction of efficiency here unimportant.

Figure 3 shows a comparison of motor efficiencies of two 125-horsepower, 600-volt traction motors. *A* has a rated speed of 2,440 revolutions per minute and maximum speed of 4,500 revolutions per minute. *B* has a rated speed of 800 revolutions per minute, maximum 1,800 revolutions per minute.

Better Commutation

The modern high-speed motors have better commutation than their predecessors. This is partly inherent in the higher-speed design and partly due to increased knowledge. The so-called "reactance" voltage that tends to cause sparking at the brushes is dependent mainly on the number of armature conductors, armature size, and speed. The higher-speed armature has less conductors and is smaller, and this more than offsets the effect of increased speed. The use of the full number of commutating poles and brush arms, as well as greater care to avoid saturation of the commutating-pole magnetic circuit at heavy loads, have also been factors in producing improved commutating and flashing characteristics. Figure 4 shows a comparison of reactance or "sparking" volts for a low-speed and a high-speed motor.

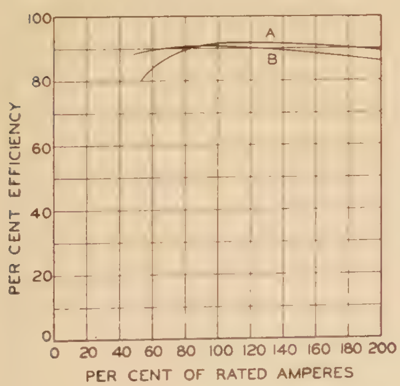

Figure 3. Comparison of motor efficiency

A. Rated 600 volts, 125 horsepower, 2,440 rpm

B. Rated 600 volts, 125 horsepower, 800 rpm

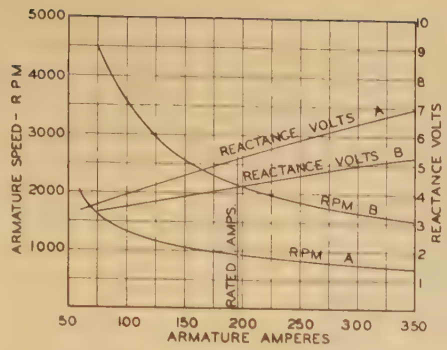

Figure 4. Commutation comparison

A. Motor rated 600 volts, 140 horsepower, 930 rpm

B. Motor rated 600 volts, 140 horsepower, 2,000 rpm

Improvements in Mechanical Construction

Mechanical improvements have gone along with motor speed increases. Some of them have been stimulated by the higher speeds. Examples of these are accurate dynamic balancing, better commutator seasoning, and more accurate commutator surfacing. Once shop methods for performing these operations were established, they were found to be as easy to do as the less adequate methods formerly used for slower-speed armatures. The types of motor mounting have been simplified so that motor frames have become simple cylinders of rolled steel. Easily removable, simple commutator covers are used, allowing easy inspection of commutator and brush holders. Motors are being mounted so that they have the same spring-supported ride as

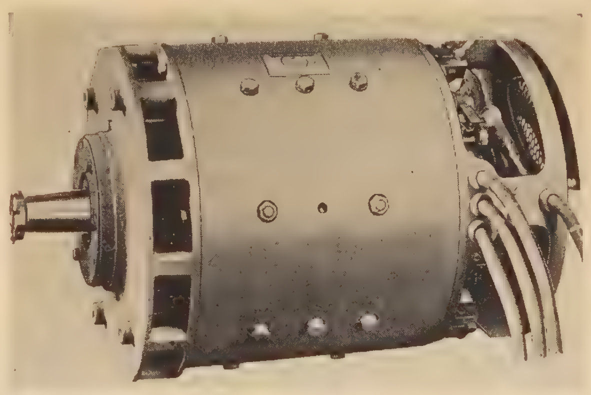

Figure 5. High-speed 125-horsepower 300-volt motor for use with double- or triple-reduction gearing on Diesel-electric switcher locomotives

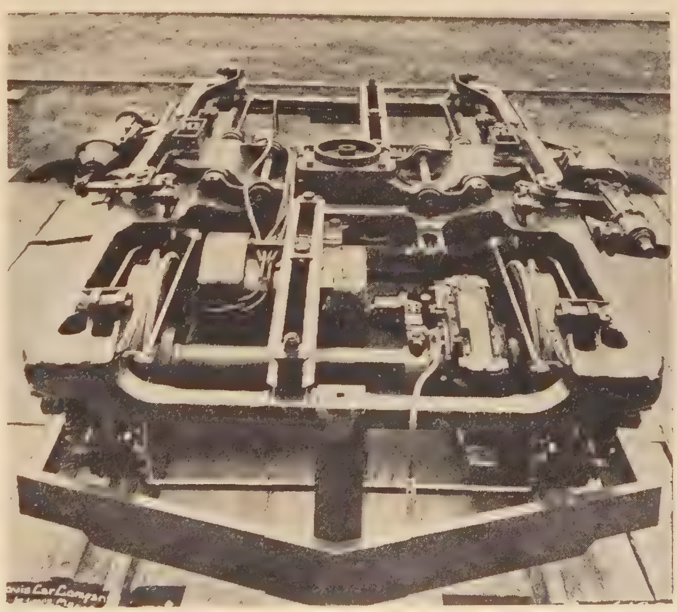

Figure 6. High-speed motors and W-N gear unit mounted in truck of high-speed trolley train

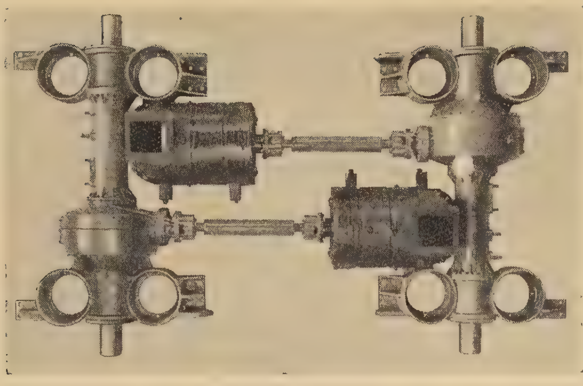

Figure 7. Mounting arrangement of motors, propeller shafts, gear units, and axles in Presidents Conference Committee streetcar truck

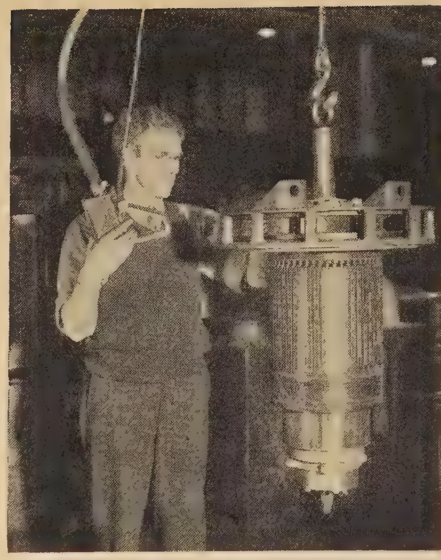

Figure 9. Armature of 140-horsepower trolley-coach motor complete with bearing housings and bearings, weighs only 525 pounds

the passengers. This permits constructional simplification not allowable on a motor receiving direct impacts through wheels and axle. An example of this is the elimination of field-coil springs. Field coils are now made accurately to size and clamped solidly between a flat washer at the pole tip and the frame at the base of the pole. In the design of the stator, any improvement that decreases the frame diameter for a given armature diameter pays almost double in weight reduction. It not only reduces the amount of iron in the magnetic circuit but shortens the length of magnetic path, thus rapidly reducing the field turns required for the same flux.

Improved Insulation

Improvements in insulating materials and treatment processes have been numerous during the past few years. These improvements have been incorporated in the new motors as they were designed. Class *A* insulation is rarely considered in the design of new traction motors. Class *B* conductor coverings, tapes, and slot wrappers have been improved so that they are thinner, yet have increased dielectric and mechanical strength along with the ability to withstand high temperatures. Improved insulating varnishes and methods of treating coils before and after their assembly have made windings less liable to grounds.

Ease of Maintenance Increased

Ease of maintenance and therefore the cost of maintenance have been considered constantly in the design of modern high-speed traction motors. The reduced labor of handling because of the smaller size of complete motor and parts is an important factor. The use of grease-lubricated roller or ball bearings and a bearing housing construction that permits removal from the frame without exposure of the bearings or lubricant to dirt is another. With the present bearing and shaft construction, shaft breakages are practically unknown and bearing replacements are very infrequent. The cost of renewal parts and the difficulty of their replacement both have been reduced in this type of motor.

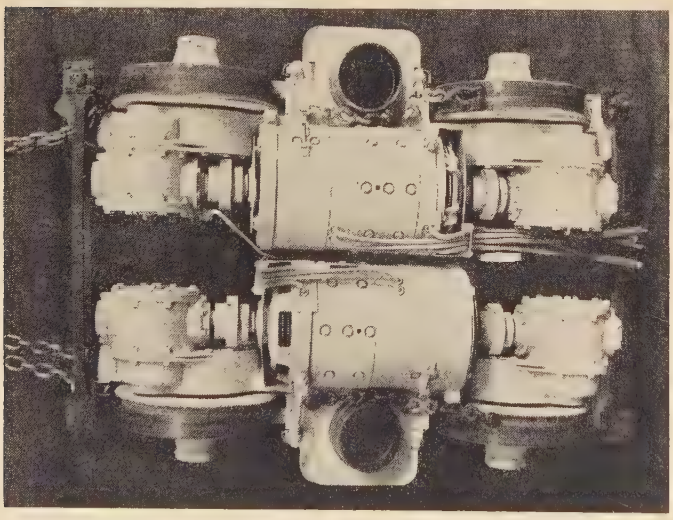

Figure 8. High-speed traction motors mounted in Differential Car Company "axleless" truck for mining locomotives

Conclusion

The modern high-speed traction motor as used today may be said to be the product of an evolutionary development and is based on sound engineering fundamentals. The outstanding advance that has been made in reduction in weight and size has been accompanied by better commutation, increased ventilation, higher efficiency, and improved mechanical construction. The various improvements that have accompanied the increase in speed have combined to produce a motor that is easier to place in a truck or chassis and is more trouble-free and easier to maintain than its predecessors.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1943 Supplement to Electrical Engineering—Transactions Section

Pilot-Wire Relaying on a Metropolitan System

T. G. LeCLAIR
FELLOW AIEE

E. L. MICHELSON
ASSOCIATE AIEE

A NUMBER of papers have been written which discuss individual pilot-wire relaying schemes. A recent paper presented by the relay subcommittee of the committee on protective devices[1] analyzed the pilot-wire circuit used in these relaying schemes. The present paper is a discussion of pilot-wire systems on a large metropolitan system where they have been in successful use for many years. The paper brings out the fact that pilot-wire relaying has many advantages over other forms of relaying because of selectivity and high-speed operation. It has also been found that the reliability of a complete relaying scheme is substantially better than would be expected from the record of the pilot-wire circuit as reported in the subcommittee paper at the 1943 winter technical meeting.

The general use of pilot-wire relaying on the metropolitan system referred to was necessitated by some special characteristics of this system. The system has a large amount of generating capacity closely interconnected through short transmission lines. Therefore, phase isolation is used to reduce the short-circuit duty on circuit breakers and limit physical damage from faults. This isolated phase system includes the use of neutral resistors which limit the ground fault current to such low values that the tripping time would be too slow with conventional overcurrent relaying schemes. The use of pilot-wire relaying permits accurate selectivity with these low fault currents regardless of the switching arrangements of the transmission lines for various operating conditions.

Practically all of the large industrial customers are served by cable loops out of stations or substations. The large number of customers served by a single loop would require excessive time settings on overcurrent relays. Therefore, pilot-wire relaying was selected to permit an indefinite number of steps without increasing the relay timing. Figure 1 shows the gain in relay time obtained by the use of pilot wires on the loop system. The overcurrent relays at the substation end of the loop (see Figure 1*b*) provide back-up protection; those shown in the center section provide primary protection for this section and at the same time limit outages caused by back-up operations to one half the loop.

On this power system, where large amounts of power are transmitted over relatively short distances, the transmission lines are of unusually low impedance. For this reason the use of distance relays is not feasible. The shortness of the transmission lines also favors pilot wires because the cost of pilot wire between stations, when added to the cost of relays at the terminals, results in a total cost for relaying comparable with the cost of distance or directional relays.

The paper includes an economic comparison to indicate that for reasonably short transmission lines, pilot-wire relaying is more economical than carrier relaying in addition to having inherently simpler methods of obtaining selectivity.

Description of Relaying Systems

At the present, a number of different pilot-wire schemes are available for transmission-line protection.[3,4,5,6] These schemes vary considerably in method, some using a direct comparison of currents at the two ends of the line, while others use a derived d-c pulse to effect the comparison. This paper describes only those pilot-wire schemes which are being used on the power system under consideration. The first published reference to these schemes, one of which has been used on this power system for the last 30 years, is a paper by R. F. Schuchardt in 1917.[7] These are circulating current schemes in which alternating current flows through the pilot wires. While all installations are of the same general type, they can be classified in the following two groups:

Scheme 1 (Figure 2) is used on transmission lines consisting of three-conductor underground cable and operates for ground faults only. Under normal conditions, there is no residual current in the line with the result that there is no current in the pilot wires. Upon the occurrence of an external ground fault, current flows through the pilot wires, but the circuit is so devised that in each relay the currents in the two coils will be equal and opposite, with the result that the relays will not operate on an external fault. For an internal ground fault, the current through the two coils of each relay will not balance, and the relay will operate. A description of the current flow for external and internal faults is given in the appendix. The relays used on this scheme are simple two-coil balanced differential relays which require $1^1/_2$ amperes difference in current to operate and trip in approximately two cycles. On most lines to which this system is applied, the current setting corresponds to a primary fault current which is lower than the full load current of the line. Recently, a new telephone-type relay was developed for this system which operates in one cycle with the same current setting.

Scheme 2 (Figure 3) is similar in principle to the first scheme except that a special pilot transformer is used instead of a two-coil relay. By the use of four pilot wires and three sets of terminal equipment as shown, the scheme protects for all types of phase

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Paper 43-79, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 8, 1943; made available for printing May 5, 1943.

T. G. LeClair is supervising development engineer, Commonwealth Edison Company, Chicago, Ill. E. L. Michelson is planning engineer, engineering department, Commonwealth Edison Company, Chicago, Ill.

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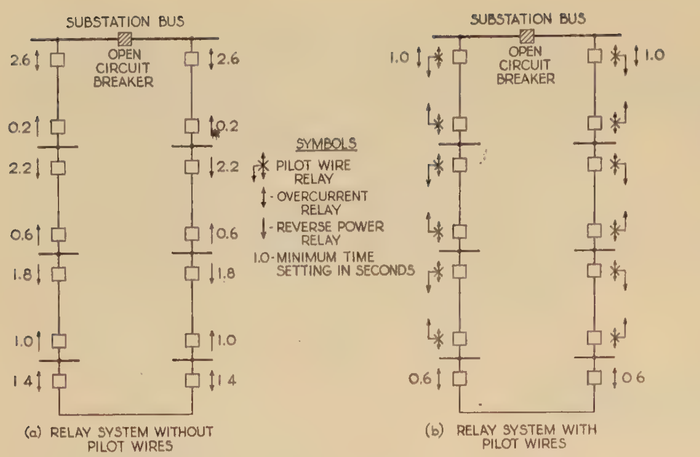

Figure 1. Effect of pilot-wire relaying in reducing relay time settings on a loop transmission system

and ground faults. The special transformer serves two purposes: first, the differential current is obtained from the transformer and, second, the current supplied to the relay is stepped up in value by the transformer. This special transformer makes possible the use of standard overcurrent relays and also results in a lower burden on the main current transformers, since a smaller value of current can be circulated through the pilot wires. This scheme is used on high-voltage lines consisting of single conductor underground cables and operates for all types of phase and ground faults. The high-voltage lines on which this scheme is used are considerably longer than the lines protected by scheme 1. This situation made it advisable to reduce the burden on the current transformers, and the special transformers were developed for this purpose. Most of these relay installations are set to operate at 0.7 ampere which corresponds to a primary fault current below full load current for the line. The latest installations of this system use high-speed relays operating in one cycle. The flow of current in the pilot wires is not shown for this scheme, but it is similar to that shown for scheme 1 in the appendix. It will be noted that the resistors used for this scheme are one half the value used in scheme 1. This is necessary because the circulating current must pass through two resistors in series at each terminal for all types of faults.

While the two schemes shown are the basic ones employed, a modification has been adopted to make possible the use of the two-wire scheme to protect for all types of faults. The method by which this is accomplished is shown in Figure 4. This method is applicable both for scheme 1 and also a two-wire scheme using pilot transformers. The principle advantage of using four pilot wires and three relays instead of two pilot wires and one relay is that the relay target will identify which cable has failed in a transmission line using single-conductor cables. The modified scheme is used to protect three-conductor cables on tie lines between important substations.

For scheme 1, which requires two pilot wires, three-conductor, number 12 pilot-wire cable is used. This is lead-covered underground cable with $^{3}/_{64}$-inch rubber insulation rated at 500 volts. All new pilot cables are tested at 3,000 volts, alternating current, for one minute. The third wire in this cable is a spare and is used in routine testing of the relays. For scheme 2, four wires are needed and five-conductor, number 12 cable is used of the same type as used for scheme 1.

It is interesting to note that in all the schemes described the relay operation is independent of the source of fault current. Relay operation is the same whether the total fault current comes from either end or from both ends. The relay operating currents at both ends of the line are equal and are a direct function of the total primary fault current. This condition holds even for the line open at one end.

A scheme was developed for neutralizing the effect of the capacitance of the pilot wires, but it has not been necessary to apply the scheme for this company. The longest line to which the relay system is now being applied has a length of approximately 15 miles, and even for this line the capacitance of the pilot wires has no appreciable effect. Charging current of the main transmission line inherently appears as fault current to the relay system, and with long, underground, high-voltage cables this factor is of some importance. For the long line previously mentioned it was found necessary to use a one-ampere setting of the relay instead of 0.7 ampere used on shorter lines.

Another characteristic of the schemes described is that it is desirable to use similar terminal equipment at both ends of a line. Correct operation of the system depends on a definite division of the secondary currents, which, in turn, depends on the relative impedance of the terminal equipment. The use of dissimilar terminal equipment might result in a loss of sensitiveness, or the possibility of tripping on through faults.

Obviously, all of the schemes described depend on the pilot circuit being in sound condition for correct operation. Either an open circuit in the pilot wires or multiple grounds on these wires will result generally in incorrect operation. For the schemes which operate for phase faults, trouble in the pilot wires will result in tripping the line on heavy load currents when there is no fault. This is not serious since it results in only one outage in a group of lines. For the schemes limited to operation on ground faults, trouble in the pilot wires will result in tripping of the line for an external fault. In all schemes, the special case of a short-circuit between pilot wires near the center of the line will prevent tripping for an internal fault on the power line.

In recent years, the pilot-wire systems have been used to protect lines which are directly connected to transformers without a primary circuit breaker. The method employed is to use the pilot-wire system to protect the high-voltage line, and standard percentage relays to protect the transformer. The operation of the transformer relay open-circuits the pilot wire with the result that the line breakers at the far end of the line will open for a small value of fault current.

This principle is also employed in some cases to trip the far end of a tie line between important substations. The location of the current transformers used in the pilot-wire scheme is such that the zone of protection of the pilot-wire relay does not cover all the terminal equipment of the line. In many cases a fault-bus or overcurrent relay is available which protects the equipment not covered by the pilot-wire relays. In these cases, the operation of the fault-bus or overcurrent relay unbalances the pilot-wire system,

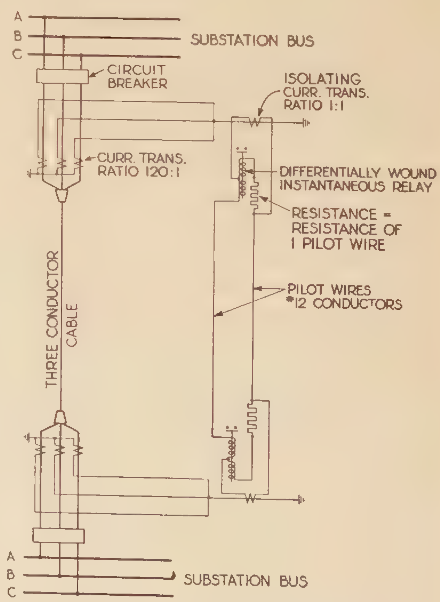

Figure 2. Scheme using two pilot wires and protecting for ground faults only

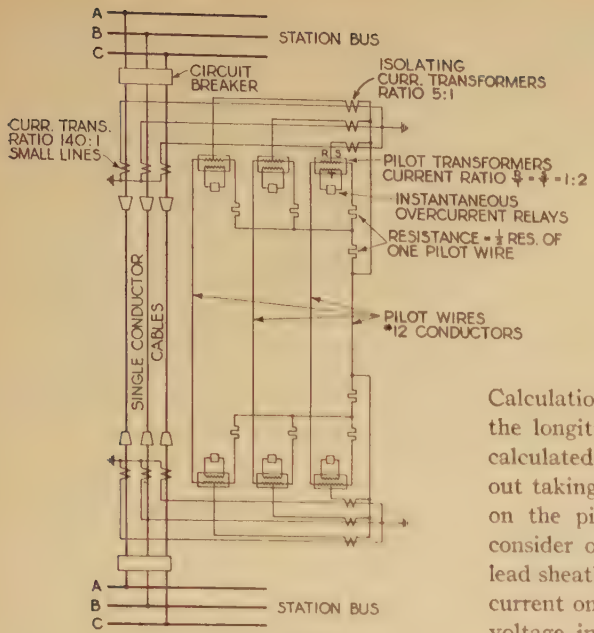

Figure 3. Scheme using four pilot wires and protecting for all types of faults

thereby causing the far end of the line to be tripped.

Abnormal Voltages on Pilot Wires

As shown in Figures 2 and 3, the pilot-wire systems are isolated from all ground connections. With this arrangement, one ground on the relay system will not cause an operation of the relays. Also, the longitudinal induced voltage on an unfaulted pilot cable is so distributed that at each end of the cable one half of the total induced voltage is impressed from conductor to sheath.

In general terms, the longitudinal induced voltage is a voltage induced along a conductor, which builds up a voltage between the conductor and ground. In the underground power system being discussed in this paper, the longitudinal voltage induced under fault conditions has a somewhat different character. In this system practically all the current in a ground fault returns to the source over the lead sheaths in the duct run carrying the faulted cable. The sheath of the pilot cable is connected to the sheaths of the power cables at each manhole, and the entire group of sheaths is practically at ground potential throughout its length.

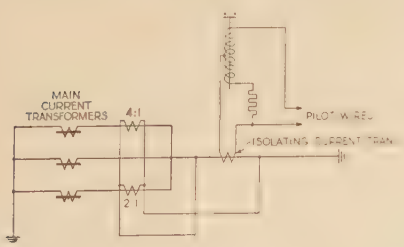

Figure 4. Method for modifying pilot-wire circuit to make it operative for phase-to-phase and phase-to-ground faults

Calculations and tests have shown that the longitudinal induced voltage can be calculated with sufficient accuracy without taking into account the flux linkages on the pilot cable. It is necessary to consider only the resistance drop in the lead sheaths caused by the return of fault current on this sheath. The longitudinal voltage in this case is the difference of potential between the two ends of the pilot-cable sheath. Also, it is the voltage that would appear between conductor and sheath at one end of the cable if the conductor were connected to the sheath at the other end. As noted previously, if the pilot-cable insulation is in good condition, one half of the longitudinal voltage would appear between the conductor and sheath at each end of the cable.

All terminal equipment of the relay system is tested at 1,500 volts a-c for one minute, when placed in operation. The isolating current transformers are tested at 2,500 volts a-c for one minute. As previously noted, new pilot cable is tested at 3,000 volts a-c for one minute.

The low values of ground-fault currents which are obtained in this power system, combined with the fact that all transmission lines and pilot wires are in lead-covered underground cable, result in very low values of induced voltage on the pilot-wire system. Calculations and tests have shown that on practically all lines the maximum induced longitudinal voltage is considerably less than 1,000 volts. For about five per cent of the transmission lines, the induced voltage is above 1,000 volts, and in the maximum case there are approximately 2,500 volts induced. However, even in this maximum case, the effect of operating pilot wires ungrounded results in the voltage stress on the pilot wires and terminal equipment being within their insulation strength.

In many cases, the rise of ground bus potential under fault conditions, caused by high grounding resistance, creates a problem in the protection of pilot-wire systems.[2] However, on this power system this effect is practically negligible. As stated before, most of the fault current returns to the station over lead sheaths, which, in turn, are directly connected to the ground bus at the station. This arrangement, combined with the fact that the total ground fault current is relatively small, results in extremely low values of ground bus voltages in the station supplying the fault current.

As a result of the low values of induced voltage on the pilot-wire cables, it has not been found necessary to use any equipment on the cables to protect against transient overvoltages.

Comparison of Privately Owned and Leased Pilot-Wire Circuits

One important question that always arises in the application of pilot-wire relays is the use of privately owned pilot wires versus leased telephone wires. Privately owned circuits are relatively high in cost; on the power system under consideration, the pilot cables used cost up to approximately $1,500 per mile installed. No cost for ducts is included in this figure because pilot cables are generally installed in center ducts which do not have good heat-radiating capacity for power cables. This cost for privately owned circuits compares with a cost for leased wires of approximately $60 per mile per year, in the metropolitan area.

Privately owned wires have several operating advantages. They make possible the use of lower resistance wires which are preferable for the circulating current schemes described in this paper. Also private cables are of higher insulation strength and, therefore, are less susceptible to the effects of induced voltage. These circuits are better protected from external damage and their service records show the benefits gained. Over a period of ten years, on this power system, there have been only five service failures on 218 circuits, consisting of approximately 300 miles of cable. This power system uses leased telephone wires on a telemetering system which consists of approximately 120 circuit miles. In a two-year period

Table I

Relay System	Per Cent Incorrect Operations	Remarks
All classes	3.7	Majority of relays, simple induction type
Used on high-voltage system	7.2	Includes many of the more complicated types
Pilot-wire schemes	3.7	Speed of operation faster than other relays on high-voltage system, in many instances

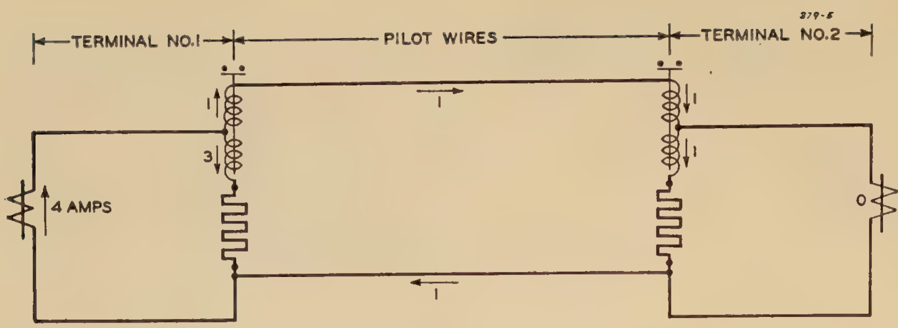

Figure 5. Current flow for internal fault with all fault current supplied from terminal 1

there have been eight emergency outages on this system, the duration ranging from a few minutes to five hours.

Data on Operating Record of Pilot-Wire Circuits

The number of pilot-wire circuits now in use on the power system under consideration, are as follows:

Scheme 1	155 circuits
Scheme 2	25 circuits
Modified scheme 1	38 circuits
Total	218 circuits

The total length of cable involved on these circuits is approximately 300 miles. During the last ten years there have been 164 operations of the pilot-wire relay systems, of which six have been incorrect. Of the six cases of incorrect operation, one was caused by terminal equipment and five by trouble on pilot cables. Following is a detailed analysis of these six incorrect operations:

In one case a terminal resistor was damaged by overloading during the testing of the relay system. As a result of this damage, the resistor was open-circuited and the line tripped out on a through fault.

In one case a pilot cable was cut by mistake by an employe of another company, and the line opened on load current. In the process of locating the trouble in this pilot cable, a company employe cut another pilot cable by mistake with the result that a second line opened on load current.

In one case a street lighting circuit failed and caused the burning of a pilot cable, which caused an opening of the transmission line.

In two cases a pilot cable was damaged and the conductors became short-circuited, with the result that the lines tripped on through faults.

In all cases of incorrect operation, the lines were tripped out either on load current or on external faults. In the cases where the line tripped on load current, service was not affected because the line which was tripped out was only one of a number operating in parallel. This situation also applies to those cases where a line tripped on through fault. Therefore, none of the incorrect operations resulted in service interruptions.

The high degree of reliability of the pilot wires has made it unnecessary to use any form of continuous supervision to check the continuity of the wires. The wires are checked semiannually as part of the routine relay test.

A comparison of results obtained by different methods of relaying requires consideration of the relay speed necessary on the different parts of the system. On this power system, the largest number of line relays is on the intermediate voltage transmission system where lines are short, stability is not a factor, and a simple time selection scheme is satisfactory. On the high-voltage system, including all lines in the interconnected group, the power concentration is greater and stability is a factor on the long overhead lines. For this high-voltage system, high-speed relaying is essential, and modern distance and carrier relays are being used in these installations.

Table I is a comparative record to show relay performance on this metropolitan system over a ten-year period.

In addition to the service outages referred to there are a relatively large number of planned interruptions of the pilot-wire systems. These outages are occasioned by the necessity for taking pilot-wire cables out of service for cable rearrangements and other types of work. These interruptions amount to a total of approximately 75 outages each year, with an average duration of about four hours. However, these outages are not important because they generally come at a time of the day when the line can be taken out of service without jeopardizing the service to customers. In many cases the pilot-wire cable can be taken out of service without opening the transmission line, relying on back-up protection to clear faults that might develop during outage of the pilot-wire system.

Comparison of Pilot-Wire Systems With Carrier Systems

A brief study of the cost of carrier-current installations for relaying purposes shows that using the type of system described in this paper, pilot-wire systems are competitive in cost for lines up to approximately 20 miles in length. Using telephone wires, the pilot-wire system is competitive with carrier current up to lines of approximately 35 miles in length. The cost comparison definitely favors carrier-current relaying on longer overhead transmission lines.

Pilot-wire relaying of the type described in this paper is inherently simpler than the carrier-current systems now in use. In the pilot-wire system the comparison of conditions at the two ends of a transmission line is made directly, whereas in the case of the carrier-current relaying, the comparison is an indirect one. In carrier-current relaying it is necessary first to translate the conditions at each end of the line into a carrier signal, and the mechanism for doing so requires a multiplicity of relay elements, each of which introduces some hazard to the reliability of the entire system. It appears, therefore, that for lines where pilot-wire relaying systems can be justified from an economic viewpoint, they should provide a more reliable form of relay protection than carrier-current relaying.

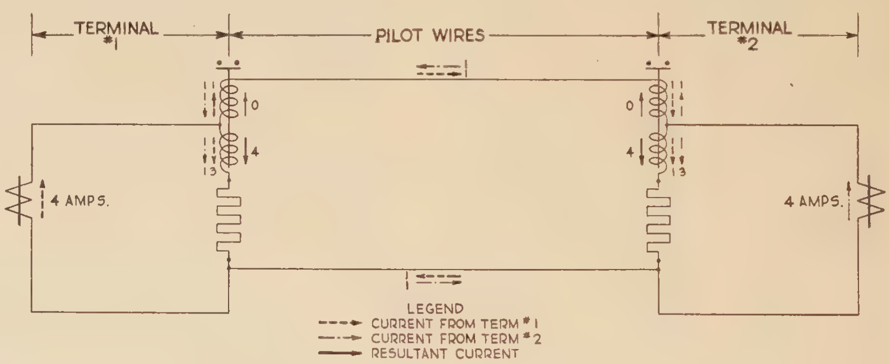

Figure 6. Current flow for internal fault with equal currents supplied from the two terminals

Conclusion

Pilot-wire relaying is applicable over a substantial field in transmisson-line protection. This scheme provides high speed and selective operation under all switching arrangements. There are many types of pilot-wire-relaying schemes which have been developed over the past 30 years. Only the schemes with which the authors have had operating experience are discussed in this paper.

Induced voltage is one of the most commonly discussed limitations to pilot-wire-relaying schemes. On overhead systems special precautions must be taken, but for this metropolitan system where both transmission lines and pilot wire are underground induced voltages have caused no trouble.

On this metropolitan system the percentage of correct operations, over the past ten years, is approximately the same as obtained from the very simple induction relay schemes. On the other hand, the percentage of correct operations is appreciably higher than that obtained from the more complicated schemes, including high-speed distance relays and carrier relays which are required for modern high-speed switching.

Pilot relaying is more economical than other schemes on relatively short transmission lines. Where pilot wires are usable, they are inherently more simple than other relays with comparable operating speed.

Appendix

Figures 5, 6, and 7 show the flow of current in the pilot wires and relay coils for external and internal fault conditions. These diagrams are based on scheme 1, but the same principles apply to the other schemes.

In Figure 5 an internal fault is assumed with all current being supplied from one end of the line. The primary fault current is assumed to be of such a magnitude that the current in the secondary circuit of the isolating current transformer is four amperes. This current divides at the point where the isolating current transformer connects to the relay coils, in proportion to the impedance of the two paths. Three amperes flow into the coil connected to the resistor and one ampere to the coil connected directly to the pilot wire. The current of one ampere flows through the pilot wire, through the two relay coils at the far end of the line, and back to the first terminal over the other pilot wire. This three to one division of current between the two coils is the same for all conditions and in all the schemes described. The ratio of the two currents is fixed by the value of the resistor in the circuit at each terminal, which in the case of the two-wire scheme is equal to the resistance of one pilot wire. With the value of the fault current chosen, the vector sum of currents in each relay for the case of Figure 5 is two amperes. Since the relays are set to operate at a net current of $1^1/_2$ amperes, this fault current is sufficient to trip both relays. Note that the resultant current at the two terminals is identical, although the fault is fed from one end only.

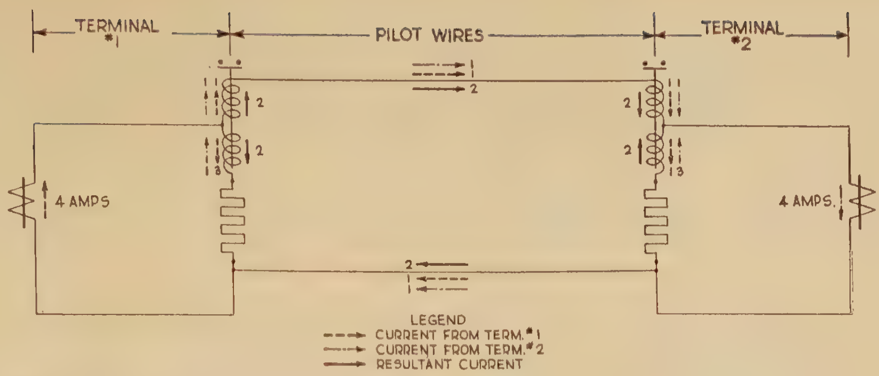

Figure 7. Current flow for external fault

In Figure 6 an internal fault is again assumed, but, in this case, there is a current of four amperes supplied from each end of the line. The simplest method of analyzing the flow of current in the pilot-wire system is to use the principle of superposition. The diagram shows separately the flow of current supplied from each end of the line. The division of current at the point where the isolating current transformer connects to the relay coils is based on the same principle covered in Figure 5; that is, three fourths of the current flows in the relay connected to the resistor and one fourth to the coil connected directly to the pilot wire. This diagram also shows the resultant of the two circulating currents, which is the actual current flowing in the system. It will be noted that the resultant differential current in each relay is four amperes instead of two as in the previous case. This illustrates the general principle that the net current in each relay is a direct function of the total internal fault current. In this specific case of equal currents fed from the two ends of the line, the pilot-wire current is zero. This condition holds only for this specific division of fault current.

Figure 7 shows the condition for an external fault with the magnitude of the current equal to four amperes in the isolating current-transformer circuit. Again, this diagram uses the principle of superposition to show the flow of current. It will be noted that the flow of current from terminal 1 has the same magnitude and direction as for the case shown in Figure 6. For the current from terminal 2 the current has the same magnitude but is opposite in direction because of the through-fault condition. The effect of this reversal of direction is to give equal and opposite currents in the two relay coils. This illustrates the general principle that for a through fault of any magnitude the currents in the relay coils will be equal and opposite, with the result that there is no tendency for the relay to operate on such faults.

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Interim Report on Overloading Distribution Transformers

AIEE COMMITTEE ON ELECTRICAL MACHINERY
Transformer Subcommittee

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication, as well as other guides and reports in this series, has been prepared for the information of users during the war emergency. Upon termination of the war emergency, they will be reconsidered by the standards committee and the committees that prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present standards have been prepared.

THE "Interim Report on Guides for Overloading Transformers and Voltage Regulators," which was presented at the AIEE summer convention, June 22, 1942, and was published in the September 1942 issue of ELECTRICAL ENGINEERING, gave information on the overloading of all types of transformers and of voltage regulators.

The interim report gave basic principles to be followed for determining permissible overloads with normal life expectancy based on the assumption that the transformer would be operated continuously throughout its life on the basis of the assumed conditions. It also gave tables and charts covering permissible emergency overloads with moderate sacrifice of life expectancy. The data given result in conservative overloads for all types and sizes of transformers.

It must be recognized, however, that such overloads may be too conservative for some small distribution transformers where the temperature rise is appreciably lower than the specified rise on account of the use of a tank larger than necessary for thermal requirements, because general rules and tables such as those given in the "Interim Report on Guides for Overloading Transformers and Voltage Regulators" must be made to fit transformers of all kinds, varieties, and sizes (within certain limits) having different design characteristics.

It is difficult to fix specific limitations for operation of distribution transformers because of the multiplicity of factors governing such operation.

For distribution applications the shape of the load curve imposed upon a transformer will vary for each application. Although for larger-sized distribution transformers the load curve may be comparable to that for a complete distribution system, for the smaller units, such as used on residential and commercial applications, the load curves are usually very complex and are not classified so readily in terms of load factor as are the load curves for power transformers.

The thermal characteristics of distribution transformers also are affected by numerous factors. Small distribution transformers inherently have greater cooling ability per kilovolt-ampere than larger sizes with the result that they have greater thermal ability. In addition, the particular design has greater influence on the permissible load than any other factor being considered. The establishment of thermal characteristics for each size, type, and manufacture of unit would be a tremendous task.

In distribution transformer application, consideration must always be given to permissible voltage drop and voltage variation in the transformers and the rest of the system as well as to the thermal characteristics of the transformers. In some cases, voltage drop rather than thermal capacity may be the limiting factor in the loading of distribution transformers.

While it would be desirable to have recommendations that would more nearly cover the ultimate load capabilities of these smaller units, specific recommendations must depend on factors peculiar to individual systems and loads to such extent that it seems impractical to produce data that can be applied generally.

The overloads for transformers recommended in the "American Standards for Transformers, Regulators, and Reactors, *C*-57.3" and in the "Interim Report on Guides for Overloading Transformers and Voltage Regulators" are based on actual load values. Various methods of estimating loads on distribution transformers are in common use. These methods do not always give results on a basis comparable to the actual load values given in the afore-mentioned reports.

It seems necessary, therefore, that those users seeking the near ultimate in loading of distribution transformers base their thoughts and conclusions on the particular characteristics of the equipment and loads peculiar to their own systems. They will find some assistance in the guide and report referred to previously and in the papers listed in the references.

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Personnel of transformer subcommittee: M. S. Oldacre, *chairman;* F. S. Brown, E. S. Bundy, represented by R. T. Henry; J. E. Clem, I. W. Gross, V. M. Montsinger, J. R. North, W. C. Sealey, F. J. Vogel, C. F. Wagner.

This interim report was prepared by the AIEE transformer subcommittee of the committee on electrical machinery for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been approved formally by the standards committee nor the board of directors.

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Adequate Electrical Maintenance Essential to Transportation

W. J. CLARDY
MEMBER AIEE

Synopsis: The demand for public transportation is increasing rapidly under present conditions of accelerated industrial activity. Adequate maintenance of electrical apparatus on rolling stock is essential to avoid impairment to the war effort. Measures which will assist to retain generators, motors, and control in service practically 100 per cent of the time are proper operation, effective inspection, preventive maintenance, keeping apparatus clean, accurate adjustments, prompt worn-part replacements, undelayed commutator conditioning, use of improved insulation, and correct lubrication. More careful attention to all of these factors during the present emergency will assure maximum rolling stock availability.

Maintenance of Transportation Electrical Apparatus and the War Effort

AMERICANS are confirmed optimists. They like to believe that life, somehow, works out for their individual good. But today, we are faced with a transportation situation which can ruin our entire internal economy just as surely as being bombed to destruction. Goods and people must be moved to places where they are needed to be effective in the war effort. Electric locomotives, suburban cars, rapid transit trains, streetcars, trolley coaches, and Diesel-electric buses are doing their bit in meeting this demand. The adequate maintenance of electrical apparatus on this rolling stock is a vital factor in providing the necessary supply of war materials.

Armies and navies are useless without the tools of battle. Materials and workers must be moved to and from industrial plants to produce these tools. The need for maximum productive capacity makes such transportation essential to the war effort. The "maintenance front" for generators, motors, and control on locomotives, cars, and coaches must be one that utilizes all available facilities expeditiously. There can be no overestimating the importance of this action or the seriousness of the situation. The maintenance of electrical apparatus on rolling stock requires planning, supervision, and execution that will assure continuity of service from every piece of equipment available.

Adequate Maintenance Needed

The steadily mounting demand for transportation has produced an acute condition, both on the city transit systems and the railroads. The total number of passengers handled by city properties in 1942 was 24 per cent above the figure for the year 1941. In the case of the railroads, passenger traffic attained an all-time record as measured by passenger miles. In 1942 (11 months) the increase was 80 per cent in comparison with a like period in 1941. A corresponding figure for the step-up in freight ton-miles is 35 per cent above the previous year. The allocations of material to build new locomotives, cars, and coaches are far below needs, and this demands maintenance standards that establish maximum availability for all existing rolling stock.

The loss of trained personnel to the armed forces and war industries contributes to the difficulties of maintaining transportation electrical apparatus. This is reflected in improper handling of equipment as well as lack of knowledge of how to make repairs. The necessity for utilizing operators and enginemen who fail to measure up to normal standards has produced a substantial increase in damaged rolling stock. Also, the number of experienced shop men is reduced, which further complicates the problem. Turnover has been stepped up, and more time is required for training new employees.

The lack of a sufficient supply of rolling stock and the constantly increasing traffic demands have established a definite need for adequate maintenance. The condition is made more serious by the decrease in the number of skilled workmen. A demand has been created for more effective planning and more dependable maintenance practices. The problem is one requiring ingenuity and resourcefulness to assure a reasonable measure of success in meeting an extremely difficult situation.

Providing Effective Electrical Maintenance

The demand for adequate generator, motor, and control maintenance requires improvements in supervision. There must be better administration and direction in order that the available man power can be used to the best advantage. It is the duty of the supervisory forces to ascertain how to get a better job done with an expenditure of the same or fewer man-hours. The time which is saved by doing a job in a more simple manner is worth as much, if not more, than if it is secured by making additions to the working force. Supervisory planning is useful as a means to avoid abusive operation of rolling stock, provide effective inspection procedures, and make full utilization of preventive maintenance.

The attainment of efficient execution of maintenance procedures, once they are established, is vital under present wartime conditions. The limited supply of rolling stock, maintenance parts, and man-hours makes it imperative that there be no waste. Those who direct the activities must follow all work closely and see that it is done in a manner conforming to practices which produce the best results. This requires a broader knowledge of progress in the industry and more attention to details than has been customary in the past. Some of the measures which are productive are keeping apparatus clean to improve operation, making accurate adjustments to insure correct

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W. J. Clardy is transportation application engineer in the transportation and generator engineering department of the Westinghouse Electric and Manufacturing company, East Pittsburgh, Pa.

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functioning, replacing worn parts immediately to prevent possible failures, conditioning commutators promptly to avoid major repairs, using improved insulating materials to prolong the life of windings, and providing correct lubrication to assure greater dependability.

Proper Operation

Abusive operation of rolling stock should be avoided since the ultimate result is lowering the standard of service provided. A schedule may be completed and a job done at the time the damage occurs, but the loss on some future occasion is likely to be many times the immediate gain. Safety can be a justification of emergency handling, or a "net" contribution to the war effort may warrant such action.

On one city property, motor failures during the severe weather of the past winter resulted in the operation of as much as 5 per cent of the equipment with three motors instead of four. A three-motor car is overloaded and the machine operating alone severely punished. The ultimate result is an increase in total failures and the number of cars out of service. Perhaps a long-range plan to use higher-grade repair materials would avoid this situation—a procedure which is proving effective on many city transit systems.

Another burden on the maintenance personnel is a substantial increase in damaged rolling stock. This is particularly true on city properties where it has been necessary to make use of many inexperienced operators. A similar condition exists in locomotive operation because of such practices as overloading Diesel-electric engines by hauling heavy tonnages at low speeds, damaging motors on switcher engines by exceeding maximum safe-speed limits, holding on grades with power on, leaving partial power on the motors after making stops, and bringing trains in with motive power partly inoperative. The trend in damaged generators, motors, and control is upward at a time when every piece of rolling stock is needed. This is a supervisory problem—one where co-operation between mechanical and transportation or operating departments has demonstrated its value in securing maximum use of coaches, cars, and locomotives.

Effective Inspection

Regularity of inspection is a definite benefit in reducing failures and lowers the over all man-hours required for maintenance work. Road failures frequently prove costly, and measures to eliminate them are essential. Too frequent attention is often as unsatisfactory as neglect. In the former case, an excessive amount of "tinkering" produces trouble, and it is better to let the apparatus alone until attention is known to be needed. One procedure under present emergency conditions is to do more "looking" and less work. However, the examination must be by men experienced in detecting possible sources of trouble.

Figure 1. Electrically propelled transportation rolling stock which mst be kept runing to assist in the war effort

Results indicate that inspection offers an opportunity for supervisory studies to adjust frequency to the exact requirements of the particular location and equipment. Naturally, there is considerable variation in needs, and schedules are fixed by items requiring the most frequent attention. Consideration of each part to determine its operating time without attention will disclose if this can be made longer. Inspection of routine items may be extended, provided the condemning limit is reached after the next scheduled examination. Changes in design to extend the inspection time of parts which establish limitations have proved helpful.

Definite inspection plans are employed which divide the work into classes based on frequency. The use of a central location for all rolling stock has demonstrated its advantages. Various jobs are specialized, and, with men trained for specific duties, man-hour requirements are lowered. Also, the provision of tools to operate on a "production-line" basis decreases the time for each job. Check lists are beneficial in securing complete coverage. Each man must know his duties, and a check list gives the details of what is required. Such lists can be worked out for individual properties to meet the needs of the equipment operated.

Preventive Maintenance

Preventive maintenance—making replacements or repairs before defects occur—has been utilized with excellent results. A part of such a program that has proved advantageous is the exact determination of the cause of failures to avoid repeaters. For example, a generator or motor may break down because of a weakness within the machine, a defect in the control, or an improper operating condition. The same may be true of a control failure. If the defect is outside the particular piece of apparatus that is injured and remains undiscovered, replacement of the damaged part is almost certain to result in a recurrence of the trouble. This happens frequently on many properties and is chargeable to inadequate maintenance.

Experience has demonstrated that "repeaters" can be eliminated. An effective system on one city property is the provision of a staff of assistant equipment engineers under the direction of the equipment engineer. Definite apparatus assignments are made, and it is the duty of each assistant equipment engineer to follow continually all items under his jurisdiction. When failures occur, or

Figure 2. Checking the length of traction motor brushes

wear appears abnormal for a particular part, it is investigated and recommendations made to eliminate weaknesses that may exist. Exceptional freedom from trouble has been attained on this property.

Overhauls constitute the backbone of preventive maintenance. Under the present difficult conditions there is a decided tendency to neglect this work. Such a procedure will ultimately lead to a breakdown on the transportation system and require "heroic" measures to re-establish satisfactory operation. Results have shown that overhauls in accord with a regular schedule are essential, and they must be complete.

Keeping Apparatus Clean

Dirt can be defined as material which is foreign to any part of a piece of electrical apparatus. For example, oil or grease are classified as dirt when they are on cables, windings, and commutators. Dust particles constantly are forming a blanket over windings and confining the heat. Dust accumulates between motor fields, plugs up armature ventilating ducts, enters armature slots, and reduces V-ring creepage distances. It serves as an abrasive on commutators and in brush boxes. Its spongelike nature permits soaking up harmful fumes, moisture, oil, and acid, thus bringing them in proximity to windings. It clogs controller cams, interferes with relay operation, causes interlock misfunctioning, and contaminates oil.

As a result of the presence of dirt, motors are overloaded, throw solder, become grounded, and require rewinding. Brushes stick in brushholders, brush spring action is impaired and commutators damaged. The reduction of creepage distances causes grounds and flashing, thus leading to failures. The dust blanket overheats control coils, damages bearing surfaces, causes relays in interlocks to misfunction, impairs cylinder operation on pneumatic switches, and clogs magnet valves. Dirt in bearing lubricant is certain to result in short bearing life.

Experience shows that keeping motors and control clean is insurance of increased availability. Man-hours are saved by care and attention to a regular cleaning schedule for removal of dirt before trouble occurs. Compressed air is used for this purpose, but care should be taken that it is dry and contains no foreign material. Exposed surfaces can be wiped with clean dry cloths after compressed air is employed. Locating ventilating intakes where clean air is obtainable and applying tight fitting covers to keep dust out of the apparatus has proven helpful. Regular overhaul of motors and control affords an opportunity for much needed thorough cleanings. This is another reason why maintaining the overhaul schedule is essential.

Accurate Adjustments

Checking apparatus to see that adjustments correspond to values that give correct functioning is necessary to secure satisfactory operation. Instruction books and check lists include data indicating what is needed to insure proper performance and avoid failures. The use of gauges wherever practicable gives uniformity and limits the time consumed in making adjustments.

Maintaining brush tension accurately is needed to secure good commutation and can be checked readily with a small spring balance attached to the brush pressure finger. Commutators must be concentric and, when deviation is suspected, can be verified with a dial indicator. Unit switches need to be free from sluggishness when closing. In the case of pneumatic types, cylinder travel is required to meet specific limits and accurate adjustment of magnet valves obtained. Finger pressure on reversers and interlocks must be maintained at recommended values. Correct relay settings, especially for overload and operating currents, contribute to protection and proper functioning. In some cases gauges have been developed for verifying gaps and spring tensions, thus speeding up relay work.

Prompt Worn-Part Replacements

Worn parts demand prompt attention to assure that none are left in service except those which are certain to run until the next inspection. Otherwise, failures in service may occur which often prove costly. The inclusion of condemning limits for various wearing parts in check lists provides a convenient reference for determining when a part should be removed. Such limits are subject to variation for different localities and types of service.

Generator and motor brushes require regular replacement, since they are in constant use. Current collection and mechanical friction cause them to wear out, the life varying with differences in operating conditions. Control parts that have maximum usage and require most frequent renewals are contact tips (main switch, cam switch, and interlock), arc

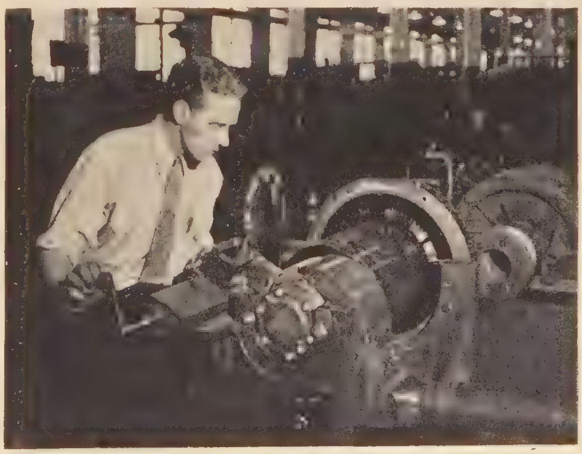

Figure 3. Method of attaching fixture to grind commutator with armature running in its own bearings

horns, and arc chute sides. There may be occasional worn shunts, damaged resistor units, and, in the case of pneumatic switches, broken cylinder springs.

When abnormal wear of parts occurs, it is an indication that an unsatisfactory condition exists. This is a warning which requires corrective action. Replacement of parts which appear to be possible causes of faults has done much to minimize maintenance work. There are items, such as brushholder boxes, brushholder pins, controller bearings, switch armature shafts, and switch hinge pins that are subject to relatively slow wear. The normal procedure is to make necessary renewals during heavy inspections or when the equipment is overhauled.

Undelayed Commutator Conditioning

The appearance of commutators is an excellent indication of the condition of the complete equipment. Poor commutation is evidence that something is wrong, and the cause may not always be within the machine. The condition is a warning, and it is usually ample since commutators seldom fail suddenly, to ascertain the reason for unsatisfactory performance. Investigation and prompt correction of conditions that produce poor commutation always reduce "maintenance man-hours." It is essential that the surface of commutators be kept smooth and true as this has a direct bearing on their life and that of the brushes. Burned spots may result because of surge currents which flow during short circuits or when power and brakes are applied simultaneously. Such spots cause the brush to leave the commutator momentarily since it is unable to follow the contour as fast as the commutator rotates. Thus, the burned spot becomes progressively worse. It is necessary to smooth the surface and bring the commutator back to concentricity.

Figure 4. Fixture to hold armature firmly in position when assembling bearings and housings

One method of grinding is by means of a rig which is attached to the frame of the machine. A generator or motor is run at slow speed during the operation. When working on a motor the wheels are jacked up and power supplied from a welding generator or the trolley. In the latter case, the motor is brought up to speed by placing the controller on the first notch. Power is then shut off, and the grinding done while the speed drops to a lower value. Light cuts are made to avoid deflection of the rig and impairment of concentricity. A dial indicator serves to check accuracy, and after the operation has been completed, the slots must be cleaned and all dust blown out of the machine. A second method for grinding a commutator with the armature in its own bearings is the provision of a fixture for shop use when the armature is out of the machine. Such devices are easily constructed and support the complete armature with its bearings and enclosures.

Improved Insulation

Insulation (class B) is now obtainable made from asbestos, glass textiles, and mica that provides exceptional durability and is far superior to the high-temperature materials of a few years ago. The life of windings is being materially increased by making substitutions for combustible substances (class A), thus stepping up heat-resisting characteristics. Advantage is being taken of high-temperature insulation when making repairs to improve the performance of older electrical apparatus. This procedure increases the "capacity margin" and assists in bettering availability at a time when every effort should be made to reduce "repair man-hours."

Dipping and baking of armatures and fields at regularly scheduled intervals prolongs the life of insulation. The frequency of such work is dependent on the conditions existing in the localities where the equipment is operated. For example, there will be differences for cold, hot, wet, and dry climates. Also the dust content of the atmosphere is an important consideration. A check of the condition of the insulated surfaces is the best indication of what is needed.

Improvement in varnishes is another factor having a direct bearing on dipping and baking practices. The heat-setting or heat-reactive synthetic-resin types represent an important development in varnish making. Curing is by heat reaction rather than oxidation as occurs with the oil types. The heat hardening produces an "all through" drying even for thick films and in restricted locations. Some of the characteristics of the varnishes are excellent resistance to moisture, oil, weak acids and alkalies; good dielectric strength; long life; ready penetration; complete drying by baking; tough

Figure 5 (left). Inspecting the accelerator and contactor group of a modern street-car control equipment

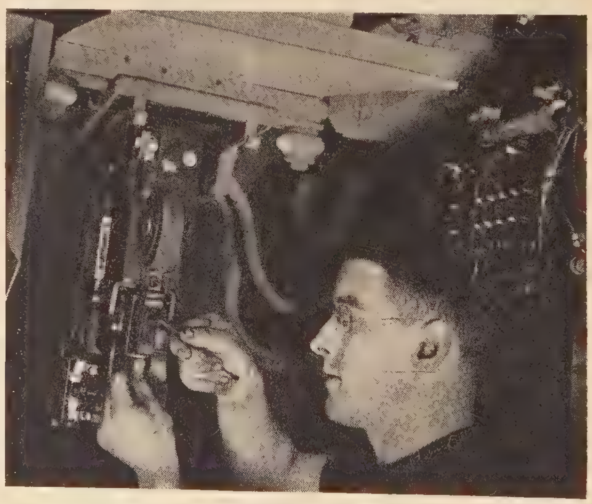

Figure 6 (right). Procedure followed in tightening the contact tips of a magnetic contactor

resilient surface film; reasonable curing temperature, and ability to withstand high class B temperatures.

The measurement of insulation resistance is a valuable tool for "preventive maintenance" and is usable to save many service interruptions. It is subject to wide variations, and the factors which influence measurements must be known and viewed in relation to each other. Temperature, moisture, test voltage, and the length of test are the important items to be considered. Standards can be fixed for various pieces of apparatus and measurements taken at regular intervals. When unsatisfactory values are observed, this is a warning that corrective action is needed. The measurement of insulation resistance will also assist in determining dipping and baking frequency.

Correct Lubrication

Care is required in the selection of the correct lubricant for various pieces of apparatus in order to secure satisfactory results. In the case of waste-packed axle and armature bearings, straight mineral oils appear best. Summer grades are required during hot weather and winter grades when it is cold, the suggested dividing line being 40 degrees Fahrenheit. This assures a more nearly constant rate of feed, since viscosity is the determining factor. Packing must be correct with a vertical wool yarn wick adjacent to the journal or shaft and a tightly filled box to hold the wick in place.

Gears and pinions require special attention to assure proper operation. The lubricant which is used must maintain its consistency and contain no nonlubricating components, have exceptional adhesive properties, be of sufficient body to withstand high tooth contact pressure, have freedom from acids, be applied easily, have enduring qualities and possess noise-deadening characteristics. Possibly the properties of stickiness and load-carrying ability are most important. There is need for the careful selection of summer and winter grades where wide temperature variation occurs.

It has been found that grease does the best job in lubricating antifriction bearings. Occasionally such bearings are allowed to run too long without lubrication, but more often the attention received is too frequent. The result of overgreasing is an increase in the operating temperature. Tolerances are exceptionally close and are comparable with those in fine time pieces. Bearings must be kept clean, since dirt means damage. When greasing, take precautions to keep the cover on the grease can, use a clean instrument to remove grease from the can, and avoid overgreasing. Housings should be one-fourth to one-half full. In the case of a new bearing, the housing should be one-third full, but the bearing space should also be filled. A relief or drain plug for use when greasing will prevent the overgreasing danger. It is desirable to apply such plugs on older type bearings when machines are being overhauled.

Lubrication of control apparatus requires restraint in the use of the "oil can." Some pieces of apparatus, such as master controllers, may be equipped with bearings requiring lubrication while others have the oilless type. For example, a typical locomotive controller requires lubrication for the upper and lower bearings of the main drums, the reverse drum bearing, and the main handle bearing pins. On the other hand, the modern street car master controller needs no lubrication. Pneumatic switch cylinders must be oiled, but the hinge pins and armature shafts of magnetic contactors require no lubrication. Strict adherence to instructions which can be included in check lists produces the best results.

Progress in Realizing Adequate Maintenance

The procedures which have been discussed give a general outline of the best means for obtaining intensified use from electrical equipment of transportation rolling stock. Many properties are benefiting from proper operation, effective inspection, preventive maintenance, keeping apparatus clean, accurate adjustments, prompt worn-part replacements, undelayed commutator conditioning, improved insulation, and correct lubrication. The availability of generators, motors, and control is being stepped up in an endeavor to keep pace with the demand. The results are creditable as a contribution to an "all-out" effort to provide essential transportation. There is need for more general use of all factors which improve maintenance efficiency in meeting the present emergency. There may be other profitable ideas which still lie dormant, and it is to be expected that the future will bring further progress.

A New Type of Adjustable-Speed Drive for A-C Systems—II

A. G. CONRAD
MEMBER AIEE

S. T. SMITH
ASSOCIATE AIEE

P. F. ORDUNG
ASSOCIATE AIEE

Synopsis: The preceding paper of this series[1] presented the theory and principles of operation of a new type of a-c motor and its control circuit. This system of speed control has the advantage of being economical from the standpoints of initial cost, efficiency of operation, and ability to correct power factor. However, these advantages are not realized unless the machine is suitably designed and its control circuit properly adjusted to supply the necessary voltages and currents. The purpose of this paper is to explain the design proportions and the adjustments of the supply voltages to provide for optimum operating characteristics.

THE characteristics of this adjustable-speed motor can be altered over a wide range by adjustment of either the magnitudes or phase relationships of the applied voltages, or by changes in the construction of the motor windings. While it may be desirable to make alterations to obtain certain characteristics, the more general problem is to make adjustments that will provide the best over-all characteristics for all speed settings. As an example, it is usually desirable to operate a motor with highest possible efficiency, but, with this machine, minimizing armature circuit resistance may ruin the speed-regulation characteristics. Thus the design proportions enter the problem from several angles, and criteria of design should not be based on one feature of the motor without regard to all consequences resulting therefrom.

In this particular system as in other adjustable-speed systems there are certain requisites that must be fulfilled to make the system practical. These requisites are as follows:

1. The design and adjustments of the machine should be such that it will operate satisfactorily for all speed settings.

2. The design and adjustments should be such that the best speed regulation is secured.

3. Throughout the load range of each speed setting currents should not be excessive. This is important in this machine because of the possibility of large no-load currents as well as large load currents.

4. The full load armature current should be approximately in phase with the armature voltage so that maximum capacity is obtained from the machine.

5. The efficiency should be high.

6. Commutation should be satisfactory.

Prevention of Overheating at No-Load

If the voltages supplied to the field and armature of this motor are adjusted to the proper phase difference at the highest-speed setting, no danger will result from the standpoint of armature heating at lower-speed settings. However, the reverse is not true. For if the phase of the field voltage relative to the armature voltage be adjusted for maximum power-factor correction at no-load for low-speed settings, the armature will overheat when the motor is operated at high speeds.

These phenomena can best be seen from Figure 1 which shows

(*a*). A circuit diagram used in supplying the motor.

(*b*). A vector diagram of the motor itself when adjusted for high-speed operation.

The dotted lines shown are for the same field adjustments (magnitude and direction of field voltage with respect to armature voltage), but with a lower-speed setting. When the motor armature is supplied with the voltage E_a, and the time phase of the field voltage is such that the flux ϕ crossing the air gap lags the armature voltage by α degrees, the generated voltage in the armature must have a locus along the line *ad*. The locus of this generated voltage must be parallel to the flux vector ϕ. The generated voltage must be of sufficient magnitude such that when it is added vectorially to the armature impedance voltage I_aZ_a the sum is equal and opposite to the applied voltage E_a shown as the distance *oa* for this high-speed setting. The voltage I_aZ_a due to the armature impedance is shown as the distance *op*.

When load is applied to the machine, the speed drops slightly. The generated voltage *ap* decreases slightly causing the impedance voltage drop *op* in the armature to shift clockwise with a corresponding shift in the resistance voltage drop of the armature shown here as the distance *oy*. Since the armature current *ox* is in time phase with the armature resistance voltage, an increase in motor load will likewise cause a shift of *ox* in a clockwise direction until the torque is sufficient to supply the load. This torque is equal to $K\phi ox \cos(\theta+\alpha)$ where K = the torque constant, *ox* is the armature current associated with the resistance voltage drop *oy*, and θ is the angle between the armature current *ox*, and the armature voltage *oa*. When load is removed, the process is reversed, and the impedance triangle *oyp* revolves counterclockwise until at no-load the I_aR_a vector *oy* and the I_a vector *ox* are perpendicular to the flux vector ϕ. For this condition the torque developed is zero. Thus for zero developed torque or true no-load on the motor, the armature current vector *ox* will always be perpendicular to the flux vector ϕ. When the voltage E_a supplied to the armature of the motor is reduced to a value *oa′* as shown in Figure 1*b*, the speed decreases, and the no-load current remains perpendicular to the flux vector ϕ. Since the angle α was not changed, the no-load value of the armature impedance voltage must be less. The new vector relations are defined by the dotted lines—that is, with an armature voltage *oa′* the generated voltage will be *a′q*, the armature impedance voltage *oq*, the armature resistance voltage *ow*, and the corresponding no-load armature current *oz*. Thus a reduction of speed caused by reducing armature voltage is associated with a reduction of no-load current, and vice versa. The phase of the field voltage with respect to the armature voltage must be adjusted so that the angle α between the flux vector and the armature voltage is sufficiently small to limit the no-load armature current for the high-speed setting to a value equal to or less than the rated current of the armature.

Adjustments for Minimum Speed Regulation

The preceding paper of this series showed that the speed of this motor decreases with increase in torque. Also, that the power factor becomes more lagging with increase in load. Change in speed with increase in load is usually undesirable in the adjustable-speed motor.

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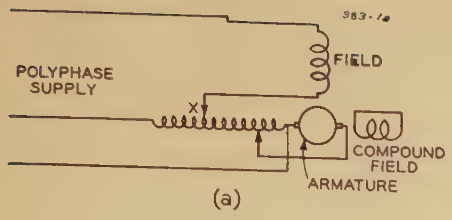
A. G. CONRAD and P. F. ORDUNG are, respectively, associate professor and instructor of electrical engineering in the school of engineering, Yale University, New Haven, Conn. S. T. SMITH, formerly a graduate student of the school of engineering, Yale University, is now at the Naval Research Laboratory, Washington, D. C.



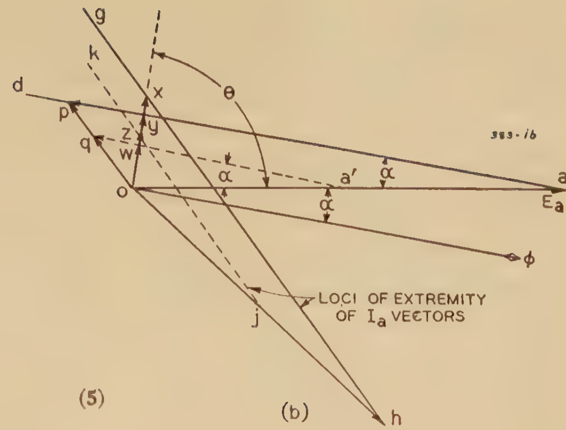


Figure 1

(a). The motor and supply circuit

(b). Vector diagram of the motor for two values of applied voltage

It is therefore desirable to make the speed regulation a minimum. Proportioning the armature circuit resistance and armature circuit reactance makes this possible. The proper proportions can be derived from the vector diagram of the motor shown in Figure 2.

The expression for the torque developed by this motor is

$$T=K\boldsymbol{\phi}\boldsymbol{I}_a\cos(\theta+\alpha) \tag{1}$$

where θ is the power-factor angle of the armature circuit considered, plus for leading power factors and minus for lagging power factors. K is a constant, and α is the angle between the flux vector $\boldsymbol{\phi}$ and the voltage vector $\boldsymbol{E}_a$.

By the law of sines

$$\frac{\boldsymbol{E}_g}{\sin(\beta+\theta)}=\frac{\boldsymbol{I}_a\boldsymbol{Z}_a}{\sin\alpha}$$

Consequently

$$\boldsymbol{E}_g=\frac{\boldsymbol{I}_a\boldsymbol{Z}_a\sin(\beta+\theta)}{\sin\alpha} \tag{2}$$

But

$$\boldsymbol{E}_g=K'\boldsymbol{\phi}N$$

where N is the speed of the motor and K' is a constant. Solving for N and substituting for $\boldsymbol{E}_g$ from equation 2, the speed

$$N=\frac{\boldsymbol{E}_g}{K'\boldsymbol{\phi}}=\frac{\boldsymbol{I}_a\boldsymbol{Z}_a\sin(\beta+\theta)}{K'\boldsymbol{\phi}\sin\alpha} \tag{3}$$

Equations 1 and 3 provide expressions of torque and speed as functions of the variables θ and $\boldsymbol{I}_a$. From the vector diagram and the law of sines

$$\frac{\boldsymbol{I}_a}{\sin\alpha}=\frac{\boldsymbol{I}_{ab}}{\sin(180°-\alpha-\beta-\theta)}$$

$$=\frac{\dfrac{\boldsymbol{E}_a}{\boldsymbol{Z}_a}}{\sin(180°-\alpha-\beta-\theta)}$$

since

$$\boldsymbol{I}_{ab}=\frac{\boldsymbol{E}_a}{\boldsymbol{Z}_a}$$

Therefore

$$\boldsymbol{I}_a=\frac{\boldsymbol{E}_a\sin\alpha}{Z_a\sin(180°-\alpha-\beta-\theta)} \tag{4}$$

Substituting equation 4 in equation 1, the expression for torque in terms of the one variable θ is obtained.

$$T=\frac{K\boldsymbol{\phi}\boldsymbol{E}_a\sin\alpha\cos(\alpha+\theta)}{Z_a\sin(180°-\alpha-\beta-\theta)}$$

Differentiating

$$\frac{dT}{d\theta}=\frac{-K\boldsymbol{\phi}\boldsymbol{E}_a\sin\alpha\cos\beta}{Z_a\sin^2(180°-\alpha-\beta-\theta)} \tag{5}$$

Substituting equation 4 in equation 3

$$N=\frac{\boldsymbol{E}_a\sin(\beta+\theta)}{K'\boldsymbol{\phi}\sin(180°-\alpha-\beta-\theta)}$$

Differentiating

$$\frac{dN}{d\theta}=\frac{\boldsymbol{E}_a\sin\alpha}{K'\boldsymbol{\phi}\sin^2(180°-\alpha-\beta-\theta)} \tag{6}$$

But

$$\frac{dN}{dT}=\frac{dN}{d\theta}\cdot\frac{d\theta}{dT}=\frac{\boldsymbol{E}_a\sin\alpha}{K'\boldsymbol{\phi}\sin^2(180°-\alpha-\beta-\theta)}\times\frac{-Z_a\sin^2(180°-\alpha-\beta-\theta)}{K\boldsymbol{\phi}\boldsymbol{E}_a\sin\alpha\cos\beta}$$

$$\frac{dN}{dT}=\frac{-Z_a}{K'K\boldsymbol{\phi}^2\cos\beta}=\frac{-Z_a^2}{K'K\boldsymbol{\phi}^2R_a} \tag{7}$$

$$\frac{dN}{dT}=\frac{-R_a}{KK'\boldsymbol{\phi}^2}-\frac{X_a^2}{KK'\boldsymbol{\phi}^2R_a}$$

From equation 7 it is evident that the slope of the speed-torque curve is a function of the flux $\boldsymbol{\phi}$ and the impedance of the armature circuit Z_a. Both of these are essentially constant for all conditions of load. Therefore the slope of the speed-torque curve is a straight line. Experiments show this to be true. Further analysis of this equation reveals that to make the slope small (low-speed regulation) the flux $\boldsymbol{\phi}$ should be large and the impedance of the armature circuit Z_a should be made as small as practicable. To determine the armature impedance angle β that will give a minimum slope to the speed-torque curve, equation 8 can be differentiated with respect to R_a. Let S equal the slope of this curve, then

$$\frac{dS}{dR_a}=\frac{-1}{KK'\boldsymbol{\phi}^2}+\frac{X_a^2}{KK'\boldsymbol{\phi}^2R_a^2}$$

When

$$\frac{dS}{dR_a}=0$$

$$R_a=X_a \tag{9}$$

and $\cos\beta$ which is equal to

$$\frac{R_a}{Z_a}=0.707$$

or

$$\beta=45°$$

Equation 7 shows that best speed regulation is obtained when the flux is large. Therefore the field circuit should be so

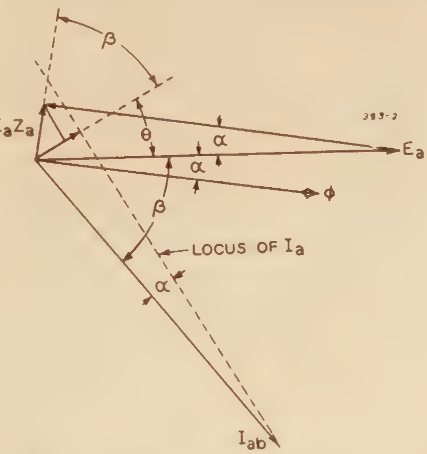


Figure 2 (above). Phase relations of currents and voltages in the vector diagram

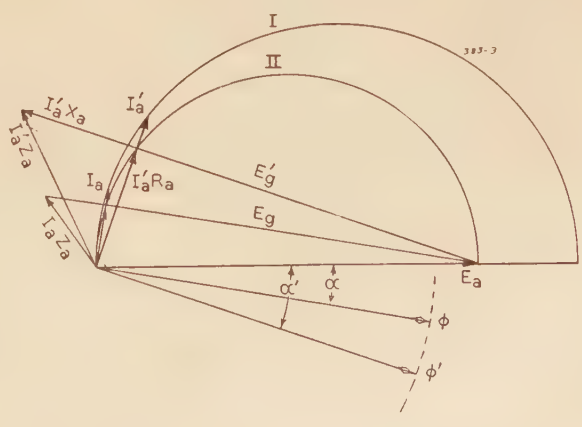


Figure 3 (right). Vector representation of the effect of a change in the angle α

I. Circle locus of extremity of the no-load armature-current vector

II. Circle locus of extremity of the no-load I_aR_a voltage vector

designed that when connected into the circuit the stator iron operates just below saturation. Furthermore, this flux should be unchanged by speed adjustment. The changes in speed can be made best by changes in the armature voltage. However, the phase angle α can be changed without changing the value of the flux. Increasing the phase angle α for a given flux changes the magnitude and direction of the no-load current. Thus in Figure 3 with the flux vector displaced by the angle α from $\boldsymbol{E}_a$, the no-load generated voltage is $\boldsymbol{E}_g$, and the no-load current $\boldsymbol{I}_a$ is at an angle 90 degrees with respect to the flux vector. If the flux is held constant and the phase angle α shifted to the angle α', the generated voltage will be changed in magnitude and direction to the position of $\boldsymbol{E}_g'$. This causes an increase in the armature impedance voltage from $\boldsymbol{I}_a\boldsymbol{Z}_a$ to $\boldsymbol{I}_a'\boldsymbol{Z}_a$. The armature current is likewise increased from $\boldsymbol{I}_a$ to $\boldsymbol{I}_a'$, and the change in direction of this true no-load current corresponds to the change in direction of the flux vector. Since the no-load current vector is always perpendicular to the corresponding flux vector, it is also perpendicular to the corresponding generated voltage vector. Since the vector $\boldsymbol{I}_a\boldsymbol{R}_a$ must be perpendicular to the vector $\boldsymbol{E}_g$ at no-load, the intersection of the vector $\boldsymbol{I}_a\boldsymbol{R}_a$ with the vector $\boldsymbol{E}_g$ must be located on a semicircle the diameter of which is $\boldsymbol{E}_a$. The extremity of the no-load armature current vector for different values of the angle α must also fall on a semicircle the diameter of which is coincident to the vector $\boldsymbol{E}_a$, and of magnitude $\boldsymbol{E}_a/\boldsymbol{R}_a$ units in length. Thus the magnitude of the no-load armature current for a given motor operated at its normal field strength will be dependent on the angle α. *This angle α should never be larger than that value required to make the no-load armature current for the highest-speed setting equal to the rated armature current. Larger angles will cause overheating at no-load.*

An analysis of equation 7 shows that a reduction of X_a to an ohmic value less than the ohmic value of R_a results in an improvement in the speed regulation. Therefore the armature circuit should be designed with the lowest leakage reactance practicable. This can be done by placing short-circuited compensating turns on the stator that are well coupled with the armature. These short-circuited turns should be well distributed, preferably in all slots. They should be made from copper straps or bars to save space and should be located as near to the armature conductors as possible. Having made the leakage reactance of the armature circuit as low as possible, the resistance of the armature circuit should be made equal to the ohmic value of the armature circuit leakage reactance. As shown in equation 9, this is necessary in order to obtain good speed regulation. If the leakage reactance of the armature circuit is greater than the resistance, the motor should have an external resistor connected in series with the armature in order to obtain the best speed regulation. The motor will then have a characteristic armature impedance such that the angle β is equal to 45 electrical degrees. The vector diagram for such a motor is shown in Figure 4. This diagram shows that when the angle β is 45 degrees and the angle α is sufficient to make the no-load armature current 100 per cent of rated value, the application of load causes the current to shift in direction until at full load the armature current op is directly in phase with the flux vector ϕ. Thus when the armature circuit resistance and reactance are made equal, the motor can correct power factor at no-load to the full extent of its rating, and under conditions of full load its current will be in phase with its flux giving maximum torque per unit of armature current.

Motors of this type can be designed so that the resistance and reactance of the armature circuit are approximately equal. If experiments indicate that the resistance is lower than the reactance, the addition of a small amount of resistance to the armature circuit may be desirable. For frequencies above 60 cycles, this may be necessary. If the motor is to be designed for frequencies less than 60 cycles, say 25 cycles, the reactance of the arma-

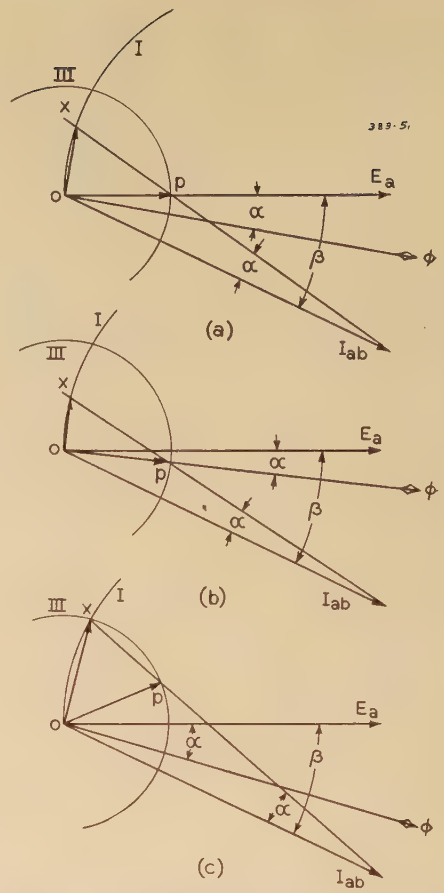


Figure 5. Vector diagram for armature circuit of the motor

(a). Angle α is 10 electrical degrees

(b). Angle α is 6 electrical degrees

(c). Angle α is approximately 16 electrical degrees

I. The locus of the extremity of the no-load armature-current vector

III. The locus of the extremity of the no-load I_aR_a voltage vector

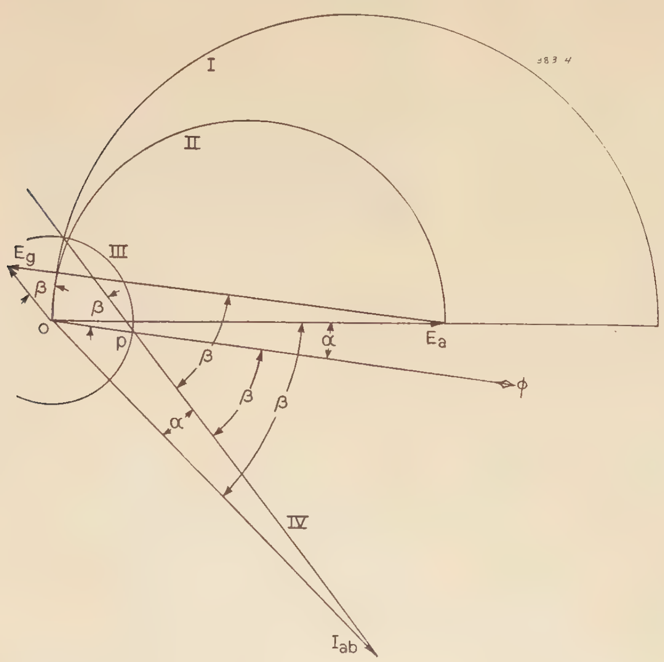


Figure 4. Vector diagram for motor having R_a equal to X_a

I. Circle locus of extremity of the no-load armature-current vector

II. Circle locus of extremity of the no-load I_aR_a voltage vector

III. Circle defining the 100 per cent value of armature current

IV. Locus of the extremity of the armature-current vector for different values of load

ture circuit may be less than the resistance. This is not objectionable, and by proper adjustment of the angle α the motor can be made to operate satisfactorily. Figure 5 shows the vector diagrams for such a motor having an armature impedance angle β equal to 25 degrees. Figure 5a shows that if the machine is adjusted to take a 100 per cent power-factor armature current at full load, it cannot correct power factor to the full extent of its rating as was possible when the angle β was 45 degrees. Figure 5b shows the diagram of the same motor as illustrated by Figure 5a except the angle α is made slightly less so that the armature will develop maximum torque with rated current. There is little difference in these adjustments since the power factor of the full load current is very nearly unity in both cases. More leading current at no-load with this same motor can be obtained by increasing the angle α as is shown in Figure 5c. The objections to this adjustment are evident from the diagram. The maximum available torque has been reduced, and consequently the horsepower rating of the machine with this adjustment is less. Thus if the armature circuit resistance is greater than its reactance, adjustment of the angle α should be such as to give conditions illustrated in Figure 5a or Figure 5b, but not those of Figure 5c.

Determination of Phase Angle of Armature and Field Voltage

The method of determining the proper phase angle between the armature and field voltage can best be explained on the basis of the vector diagram of Figure 5a. This method applies to any motor of this type having an armature circuit reactance equal to or less than its resistance. Armatures not so proportioned should have additional resistance connected in series with them to give such proportions. The steps in the procedure of making proper adjustments are as follows:

1. Determine the value of the standstill armature current I_{ab} for maximum armature voltage E_a. This can be obtained from low-voltage determinations of the armature impedance.

2. Draw the vector E_a as a reference for the maximum armature voltage to be applied to the motor and its corresponding standstill current I_{ab} at the proper angle β with respect to E_a.

3. Draw the circle locus I of the no-load armature current for different values of the angle α so that its diameter is coincident with the vector E_a and its length is E_a/R_a units in magnitude.

4. Using o as a center, draw the circle locus III of the 100 per cent value of the armature current.

5. Determine the locus of the extremity of the armature current vector by drawing a line through the extremity of the vector I_{ab} and the 100 per cent power-factor armature current op.

6. Extend the armature current locus to the no-load armature current circle. Determine intersection at X.

7. The proper value of the angle α between the flux vector ϕ and the armature voltage E_a can now be determined from the angle made between the lines representing the standstill armature current and armature current locus (the angle between I_{ab} and the line xp extended).

8. Apply maximum values of armature and field voltages to the motor such that the field voltage is approximately 90 degrees minus α ahead of the armature voltage.

9. With the motor running at no-load change the phase relation of the field and armature voltage by proper selection of tap x on the adjusting transformer until the armature current has a value corresponding to that of ox shown in Figure 5a.

This provides proper adjustment of the control circuit supplying the motor.

Efficiency

When the motor is adjusted as described in the preceding section, it will take a full load current at 100 per cent power factor. Such an adjustment will give optimum full load efficiency. Departures from these adjustments are apt to cause excessive currents because of low power factors, and damage to the machine may result from overheating. The full load copper losses of this machine are somewhat larger than those of a d-c motor of the same rating. The iron losses and the loss in the armature coils undergoing commutation tend to make the machine less efficient than the d-c motor. Efficiencies of 70 per cent have been obtained on two horsepower motors. Larger motors will have higher efficiencies. Curves of efficiency with proper adjustments on this motor are presented in Part I of this series of papers.[1]

Commutation

Commutation on this motor is comparable with that of the a-c series motor. The causes of brush sparks on this machine are essentially those caused by the inductance of the coils undergoing commutation and the induced voltages in those coils because of transformer action from the field. Since the brushes are located on a magnetic axis with respect to the stator field, they are located essentially in a neutral plane with respect to this field. Since the field is pulsating in nature and not rotating as in other polyphase a-c machines, it is much easier to obtain sparkless commutation on this motor, than on a motor which has its armature subjected to a rotating field flux.

There is a shift of flux in space with load on this motor because of armature reaction just as there is in a d-c motor. Consequently good commutation is obtained by shifting the brushes backward slightly with respect to the direction of rotation. This is satisfactory for small motors. Larger machines should be provided with interpoles.

The effect of the transformer voltages induced in the armature coils undergoing commutation can be made small in this motor by designing the armature for operation on low voltage. The control circuit makes this feature possible. By adapting a low-voltage armature to a high-voltage supply, by use of the autotransformer, and by shifting brushes slightly good commutation may be obtained.

List of Symbols

E_a—The voltage applied to the armature.
E_g—The effective value of the generated voltage in the armature.
I_a—The effective value of the armature current expressed in amperes.
I_{ab}—The effective value of armature current expressed in amperes when the motor is at standstill.
K—Torque constant of the motor.
K'—Generated voltage constant of the motor.
R_a—The equivalent resistance expressed in ohms of the armature and compensating winding.
S—Slope of speed-torque curve
T—The developed torque of the motor expressed in pound-feet.
x—The point on the adjustable transformer at which the field is connected.
X_a—The equivalent reactance of the armature and compensating winding expressed in ohms.
Z_a—The equivalent impedance of the armature and compensating winding expressed in ohms.
α—The phase angle expressed in electrical degrees between the applied armature voltage vector and the vector representing the air gap flux.
β—The angle having a cosine that is equal to the ratio of the armature resistance R_a to the armature impedance Z_a.
θ—The phase angle or power-factor angle in electrical degrees between the impressed armature voltage E_a and the armature current I_a.
ϕ—The total flux per pole crossing the air gap expressed in maxwells.

Reference

1. A New Type of Adjustable-Speed Drive for A-C Systems, A. G. Conrad, S. T. Smith, P. F. Ordung. AIEE Transactions, volume 62, 1943, January section, pages 7–10.

High-Voltage Power-Transformer Design

M. B. MALLETT
MEMBER AIEE

Synopsis: A core-type power-transformer design with shielded high-voltage winding and graded insulation has been developed for application in the higher-voltage classifications, and up to the present time more than one-quarter million kilovolt-ampere capacity of this kind of transformer has been manufactured and placed in service. The design employs a new form of construction for high-voltage power transformers which has been called "distributed concentric" construction. Outstanding characteristics of the distributed-concentric-type transformer include:

1. Reduced inherent reactance resulting in a reduction of critical materials for a given specification.

2. Ready adaptability to forced oil flow directed through the winding ducts resulting in a reduction of critical materials for a given thermal capacity.

3. Strategic distribution of voltage stress throughout the dielectric structure.

4. Simple and stable mechanical construction of windings, insulation, and shield.

POWER transformers for service in the higher-voltage transmission systems generally operate with grounded-neutral star-connected high-voltage windings, and the core-type transformer design described herein has been developed principally for this type of application.

In most core-type power transformers, as usually manufactured on this continent, the high-voltage winding of each core leg comprises the familiar stack of horizontal disc coils assembled over a conventional-type low-voltage winding. It is common practice to provide shielding and grade the insulation of these disc-coil stacks when designed for star connection and grounded neutral operation at the higher-voltage levels.[1] In general this shielding is secured by enveloping the high-voltage stack with a number of line potential shields which are so shaped and disposed as to supply each individual disc coil with its required charging current, as illustrated in Figure 1.[2] Grading of the major insulation in these transformers is ordinarily attained by special series–multiple connections of the high-voltage winding, in combination with variable diameter of individual disc coils, also illustrated in Figure 1.

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M. B. MALLETT is transformer engineer, English Electric Company of Canada, Ltd., St. Catherines, Ont., Canada.

In the core-type design under consideration, termed "distributed concentric" design, the high-voltage winding of each core leg comprises a group of vertical helical coils which are concentrically distributed between a line potential cylindrical shield and the low-voltage winding, as illustrated in Figure 2. In general the high-voltage coils of each leg are series connected between line and neutral terminals and progressively spaced from the low-voltage winding and grounded end surfaces in relation to the individual coil potentials from ground. With this distributed arrangement of the high-voltage winding, adequate shielding is secured with a single cylindrical shield on each leg of the core, and the several coil-to-coil insulations not only serve individually as internal winding insulation but also function in series as graded major insulation to the low-voltage winding and ground.

I. Evolution of Design

From the viewpoint of shielding, the distributed concentric construction employs, in effect, a concentric series of relatively long and narrow "barrel" coils which are progressively disposed between the two plates of a condenser, an obvious general arrangement suggested in principle many years ago as a means of shielding.[3] As usual, however, the gap between principle and practical application is not readily bridged. In this regard, it is well known from service experience with ordinary single barrel applications in power transformers, that conventional-type barrel-coil structures without radial spacers tend to break down between turns under conditions of short circuit. Progressive arrangements of conventional-type barrel coils are commonly used in high-voltage testing-transformer design, but here there is no mechanical problem.

According to responsible engineers* recently escaped from Europe, the Allgemeine Elektricitäts-Gesellschaft have used power-transformer-winding arrangements in Germany within the past few years which comprise

1. A concentric series of barrel coils without radial coil spacers.

2. Each coil directly supported by a wound untreated paper cylinder with slit flanged ends and cooled by an outer axial duct.

3. The complete group of coils and untreated paper layers progressively wound with one set-up to form a virtually inseparable winding structure.

It is submitted, however, that such a construction is mechanically inadequate and unacceptable in respect to service conditions on this continent, since positive turn-to-turn separation is not provided, and neither secure factory clamping nor field replacement of individual coils is feasible.

The distributed concentric structure, as developed and specifically described, is offered as a new form of construction for high-voltage power transformers. In this construction positive axial separation of conductors is provided, and individual coils may be readily clamped and handled in the shop or replaced in the field as independent units—fundamental mechanical characteristics which have been standardized on this continent for many years by conventional disc-coil construction.

It will be shown that the distributed concentric transformer possesses highly desirable electrical and thermal characteristics, including low inherent reactance and ready adaptability to forced oil flow directed through the coil ducts, and the advantages of the general winding arrangement are by no means confined to the application of shielding.

II. General Description

Figure 3 illustrates some of the available high-voltage coil connections as applied to single-phase and three-phase designs. Ordinarily from four to six coils are used for each leg, corresponding approximately to a test level range from 277 kv to 576 kv respectively. In general top-to-bottom outside coil crossovers are cumbersome and to be avoided, particularly in the case of directed forced oil flow, and some of the connections shown are unique and have been devised to meet this condition.

Each high-voltage coil employs a new form of helical coil structure, as shown in Figure 4, in which a continuous wound-in spacer of channel section functions as the key feature. High-voltage taps are located in one or more coils of each leg and symmetrically spaced with respect to the coil center line, as illustrated in Figure 5, the tap straps being carried along the face of the coil in the axial cooling ducts and then outward through radial end ducts.

The low-voltage winding may be either

* Two members of the Association of Polish Engineers in Canada who are thoroughly familiar with recent transformer practice in Germany. The names of these engineers are withheld, since their families are still in Europe.

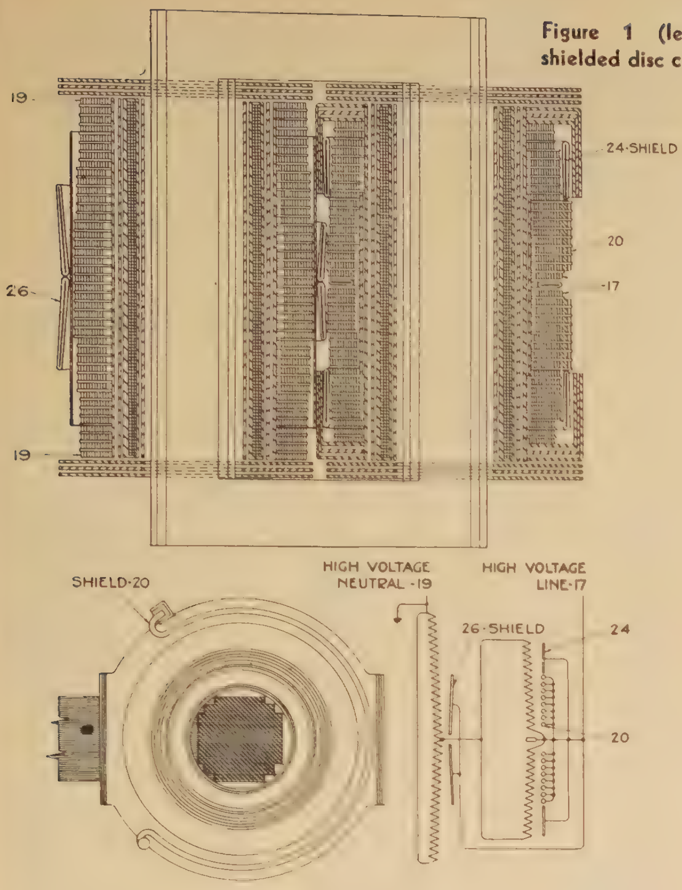

Figure 1 (left). General arrangement of shielded disc coil winding with graded insulation

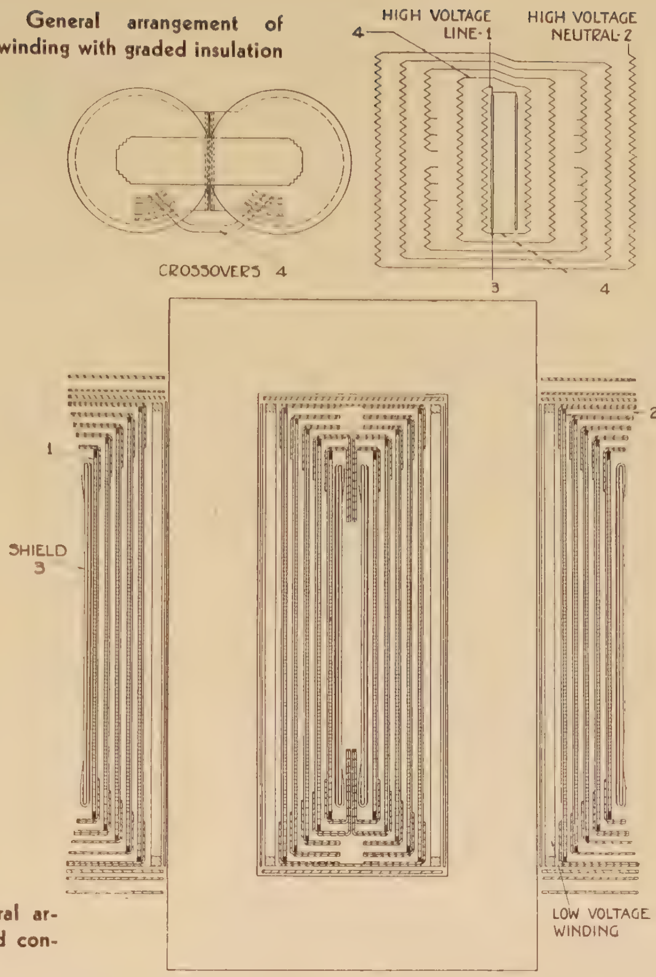

Figure 2 (right). General arrangement of distributed concentric design

of conventional type with horizontal ducts and intermittent spacers or of the same general type as the high-voltage winding. In the latter case, which is used in conjunction with directed forced oil flow, the number of coils will be substantially less than the number of high-voltage coils.

Insulation between adjacent high-voltage coils includes one winding cylinder and one axial oil duct in combination with independent flanged collars at the cylinder ends, as illustrated in Figure 4. The end insulation structure is designed around the coil-to-coil insulations, and the progressive radial end ducts are used for bringing out coil leads and tap straps.

The single shield of each core leg consists of a simple paper cylinder enveloping the outer coil and containing an embedded layer of circumferentially wound metal strip.

III. Inherent Reactance Characteristic and Associated Reduction in Critical Materials

The distributed concentric design has low inherent reactance resulting from the radial distribution of high-voltage ampere turns through the major insulation. This characteristic is illustrated in Figure 6, which compares the effective leakage width of two approximately equivalent hypothetical designs; one of the distributed concentric type and the other of conventional disc-coil construction. It will be seen that the reactance of the latter is approximately proportional to 5.5, a value more than 40 per cent greater than the corresponding figure of 3.9 for the distributed concentric design.

In this illustrative comparison, refinements in reactance calculation have been neglected for the sake of clarity, and the reactance ratio would vary somewhat from that shown, depending on the actual rating under consideration. It will be apparent to the transformer designer, however, that in general the distributed concentric construction employs a core some 20 per cent smaller in section than that required by conventional disc-coil construction for a given reactance and specification.

In addition to reducing the consumption of core steel by about one fifth, the decreased core dimensions result in a smaller volume of over-all physical structure with corresponding reductions in tank plate, clamp structure, oil, and insulation, and the total weight of critical materials is reduced by approximately one tenth. Thus, it is evident that the low inherent reactance characteristic of the distributed concentric design is of considerable practical significance, particularly under present wartime conditions when the necessity for conservation of critical materials is paramount.

IV. Thermal Characteristics and Associated Reduction in Critical Materials

The outer surface of each high-voltage coil is directly exposed to vertical oil ducts and heat flow from conductors is principally outward, although some loss is transmitted through the foundation cylinder. Thus, for any horizontal section taken through a coil, the temperature rise of each strand above local oil is readily determined by solution of an elementary series–multiple thermal circuit, once the thermal constants are established. The constants for solid insulation are well known, and a series of experimental direct-current heat runs indicated that the continuous spacer flanges, extending into the axial duct, only increase the oil-film constant of the vertical surface by about one degree for the usual design proportions. This surprisingly small effect of the spacer projections

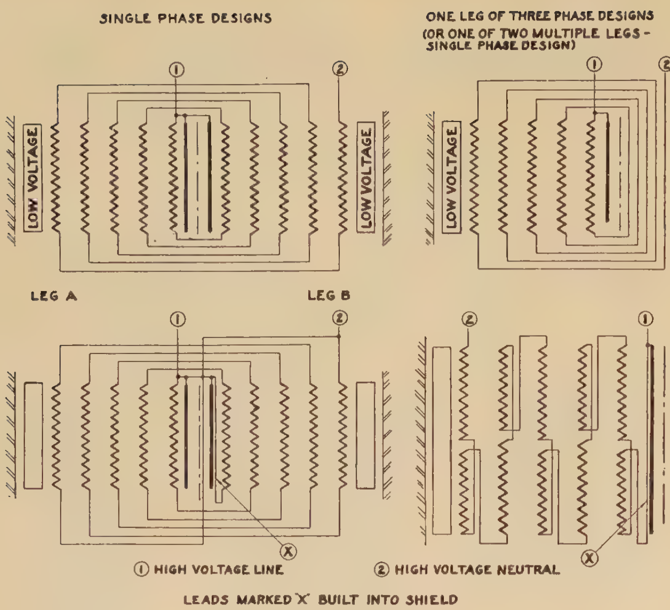

Figure 3 (above). Distributed concentric coil connections

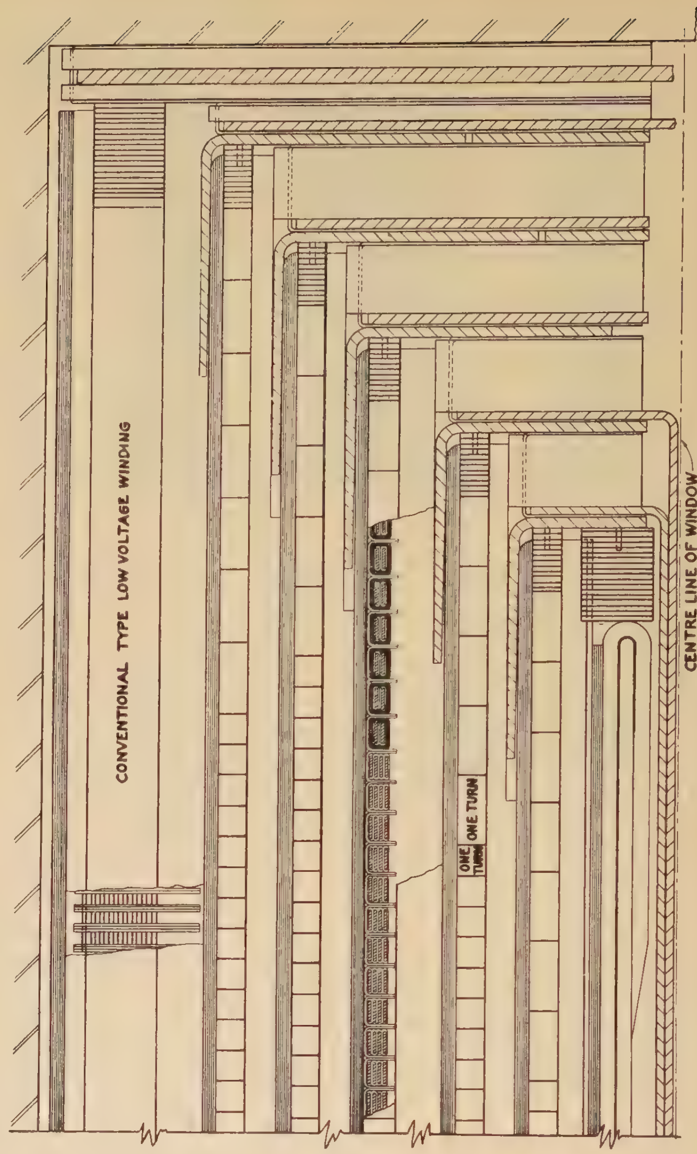

Figure 4 (right). Section through end portion of a typical structure

is apparently due to relatively high velocity eddy currents in the oil flow over the serrated coil surface. In general the relation between copper-oil gradient and load current is similar to that of conventional designs, and accepted rules defining permissible overloading of conventional-type transformers should be equally applicable to the distributed concentric design.

In order to reduce the consumption of critical materials, the application of external forced-flow heat exchangers to large transformers is rapidly increasing. With this type of cooling, a further reduction in materials results from directing the existing forced oil flow through the coil ducts, a condition readily attained in the distributed concentric construction merely by adding a simple pressboard barrier, as illustrated in Figure 7. It is evident from this drawing and Figure 4, that the inherent construction of the distributed concentric design is uniquely suited for directed-flow cooling, as contrasted with the unduly elaborate means obviously necessary in the case of conical-shaped stacks of horizontal disc coils.

The increased cooling effect of directed flow as compared to natural flow is illustrated by Figure 8, in which average and hottest copper rise above average oil are shown for both types of cooling. The curves are plotted for a typical coil, in which the increased insulation of the buffer wire is overcompensated by doubling the cooling surface per watt of the end turns, as shown in Figure 4, and the hottest strand is located in that turn of main wire which is adjacent to the upper buffer portion of the coil. Directed flow reduces the rise of the hottest strand above mean oil temperature in two ways, first by lowering the oil-film constant, and second by reducing the axial thermal gradient in the oil. The average copper rise above mean oil temperature is only affected by the first factor of reduced oil-film constant. For a given average oil temperature, the double-cause effect of directed flow on hottest copper rise is indicated by the considerable difference between curves B and B' of Figure 8, and the single-cause effect on average copper rise is indicated by the lesser difference between curves A and A'.

This substantial reduction in hot spot—average oil gradient resulting from directed flow—is important, since the actual thermal capacity of a transformer is determined by permissible hot-spot temperature. For example, a given transformer rated at 100 per cent continuous load with natural internal circulation and a given external cooler could be rerated by the addition of a simple pressboard barrier to approximately 120 per cent continuous load at the same hot-

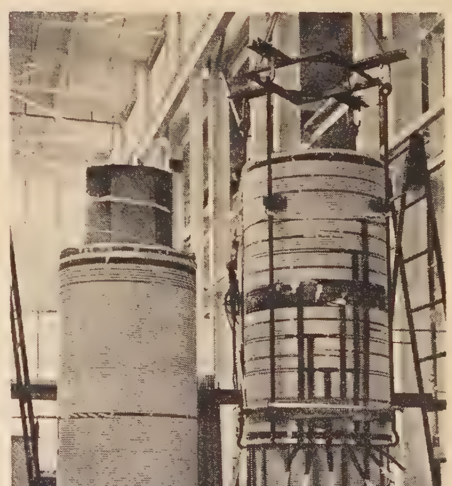

Figure 5. Lowering a tap coil into the main assembly

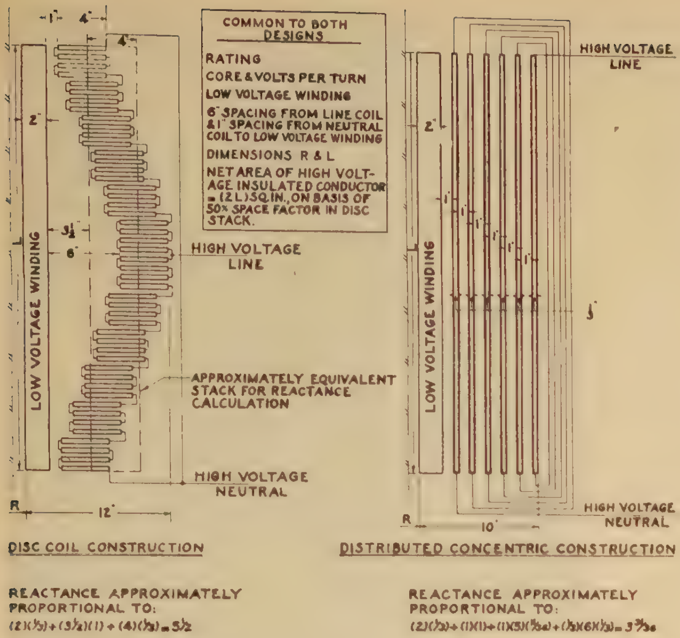

Figure 6 (left). Illustrative comparison of inherent reactance—distributed concentric versus disc coil construction

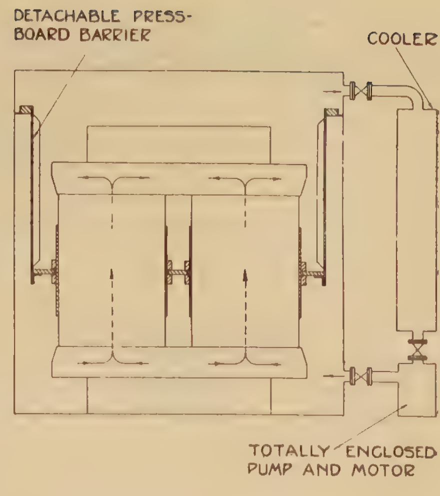

Figure 7 (right). Baffle arrangement for directing forced oil flow through the vertical coil ducts

spot temperature. Conversely, directed flow would result in a reduction of approximately 15 per cent in the total weight of critical materials required for a given continuous rating. Accordingly it is evident that the ready adaptability of the distributed concentric construction to directed forced oil flow may be conveniently utilized to substantially increase the kilovolt-amperes obtained per pound of critical materials consumed.

V. Distribution of Voltage Stress

Unless some form of shielding is provided, it is well known that the initial distribution of impulse voltage along a transformer winding departs radically from uniformity, and subsequent voltage oscillations ordinarily raise the potential of intermediate winding points to values which preclude any grading of the insulation. It is clear, however, that shielding to establish a perfectly uniform initial distribution, with absolute elimination of subsequent oscillations, is neither attainable nor necessary in practical design. The degree of shielding to be used in a transformer with graded insulation is therefore a question of economic balance and is determined by that combination of compromise shielding and partially graded insulation which has minimum over-all cost.

With a vertical front impulse applied to the line terminal, the initial or electrostatic voltage distribution along the winding of the distributed concentric transformer is fixed by the combined network of radial shunt capacities and axial series capacities, including the shunt capacities associated with the physical coil crossovers. The initial voltage distribution, corresponding to application of an infinitely long rectangular wave, is shown in Figure 9 for two alternative coil arrangements. Local oscillations of transition from initial to final straight line gradient overshoot the final gradient as an axis, and maximum voltage levels throughout the winding are indicated by the approximate ceiling shown as a straight broken line. In actual design, insulation strength is provided between parts of the winding and from winding to ground, corresponding to the maximum stresses thus created by application of an infinitely long rectangular wave. Since all real waves encountered in service must be of finite length and slope, the values of insulation strength as provided correspond to the upper

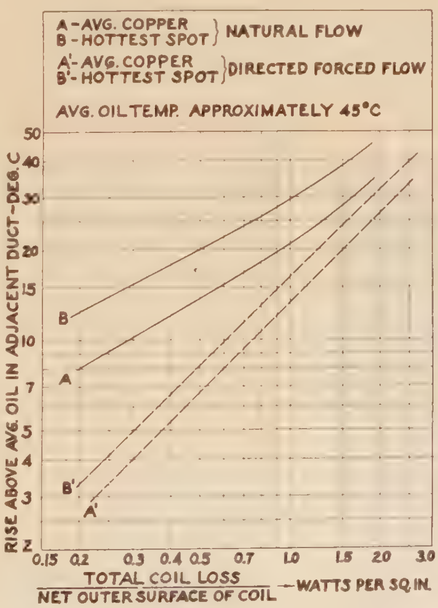

Figure 8. Effect of directed forced oil flow on copper—oil gradient

limit of stresses attainable for a given crest voltage.

The re-entrant coil arrangement included in Figure 9 was devised principally to eliminate the objectionable outside vertical crossovers of the alternative straight-coil scheme. Examination of the respective voltage distributions, bearing in mind that the final impulse distribution also corresponds to the low-frequency gradient, will show that each arrangement requires approximately the same coil-to-coil allowances. The shunt capacities associated with the internal vertical leads of the re-entrant coil scheme are controlled by adjustment in physical width of lead conductors, and these capacities are conveniently utilized to substantially improve the initial voltage distribution as compared to that of the straight coil arrangement.

Because of the physical distribution of the high-voltage winding *through* the major insulation, it is evident that both low frequency and impulse voltage stresses, as shown in Figure 9, are advantageously distributed throughout the whole dielectric structure. Adequate control of impulse voltage on each leg of the core is readily secured with a single shield surface of plain cylindrical shape. Also greatly reduced and simplified shield insulation is sufficient, since the maximum voltage from shield to adjacent winding is ordinarily less than one third of line voltage, as compared to approximately full voltage in conventional construction.

VI. Mechanical Construction Including Shop Processing

The practicability of the distributed concentric transformer hinges on the mechanical characteristics of the physical structure, which in turn depend on specific details of construction, as illustrated in Figure 4 and explained below.

In each high-voltage coil the multiple

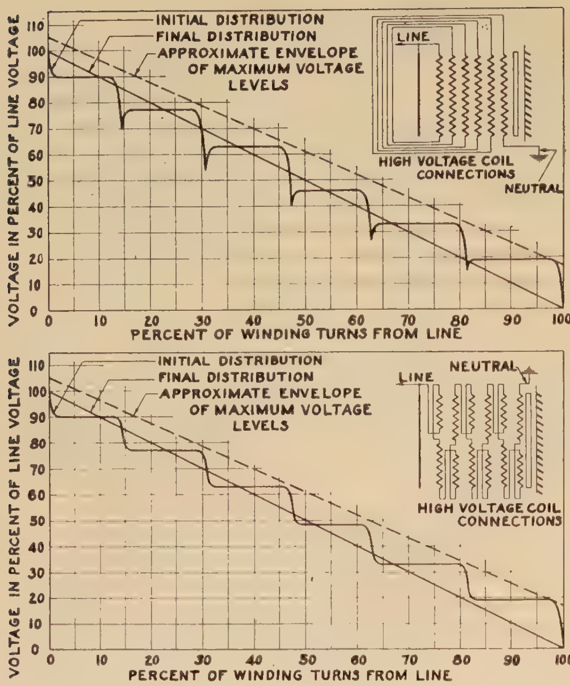

Figure 9 (left). Voltage distribution with infinitely long rectangular wave applied to line terminal

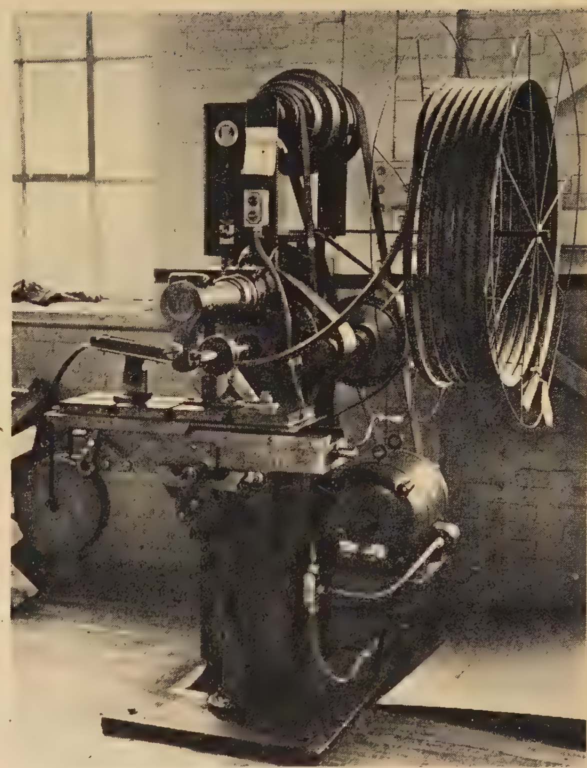

Figure 10 (right). Forming continuous channel spacer from flat pressboard strip

rectangular conductors are encased throughout their entire length by the continuous pressboard spacer of channel section, the conductors and spacer being simultaneously wound directly on a hard composition foundation cylinder. Ordinarily this spacer is $^1/_{32}$ inch thick, and the spacer flanges extend radially at least $^1/_8$ inch beyond the coil. Cross-grain pressboard end rings, in combination with the hard foundation cylinder, permit ready clamping and handling of each individual coil prior to assembly. Individual coils are progressively mounted in the main assembly as independent units, being finally supported and clamped to firm dimension by the end insulation structure which is progressively built around the independent flanged collars. The simple cylindrical shield structure is ordinarily wound on a hard foundation cylinder and also assembled in the main structure as an independent unit.

Under conditions of short circuit, the conductors of any cylindrical coil structure are subject to the formidable combination of pulsating axial force, pulsating circumferential stress, and elevated copper temperature with attendant thermal expansion. Thorough consideration of these factors and the unsatisfactory service record of conventional-type barrel coils lead to the conclusion that (1) positive axial separation of conductors, (2) firm coil clamping, and (3) freedom of coil-diameter expansion, are inescapable requirements for the prevention of conductor squirming and turn-to-turn breakdown. It is clear from Figure 4 and the preceding description that these specific requirements are all met in the distributed concentric construction.

It is further evident that all coils and parts may be disassembled and reassembled as separate independent units of the main structure. Thus, individual items, such as a high-voltage coil, could be furnished by the factory for field replacement and local damage repaired at the site without shipping the complete machine back to the shop. This feature is considered essential by most purchasers on this continent.

These fundamental mechanical characteristics, as specifically enumerated in respect to coil stability and field repair, have been standardized on this continent for many years by conventional disc-coil practice.

As further compared to conventional disc-coil construction however, the unique

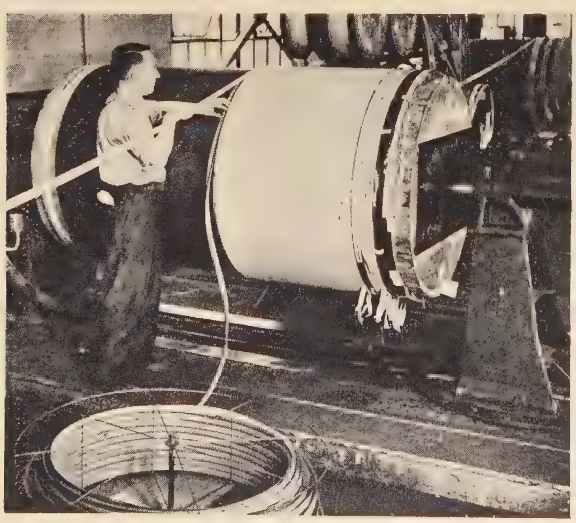

Figure 11 (left). Partially wound high-voltage coil illustrating simultaneous feed of conductors and continuous channel spacer

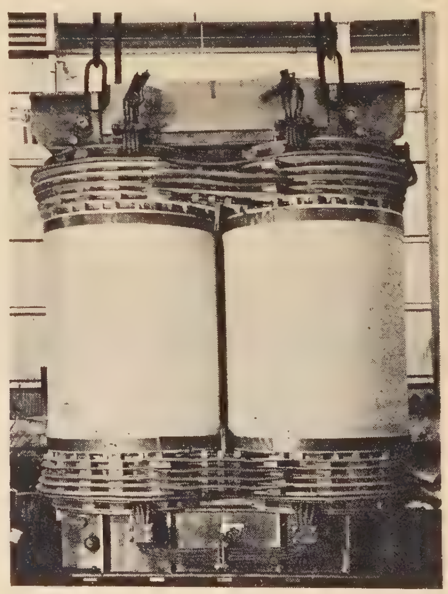

Figure 12 (right). Core and coils of single-phase 30,000-kva distributed concentric transformer, 323-kv test level

coil structure of Figure 4 comprises a solid cylindrical column of copper and insulation in which the shearing and cantilever actions of conical shaped disc-coil stacks are eliminated. Also the bearing surface between turns is continuous and uniform without the compression stress concentrations associated with intermittent radial spacers, and the entire cross section functions in the transmission of axial forces. It is further evident that the continuous-spacer feature eliminates the possibility of misaligned spacer columns, and the hazard of cutting the conductor insulation at the spacer edges. Intermittent radial spacers are used in the supporting-end insulation structure, but the continuous-bearing feature within the coil is maintained at the coil ends by the beam strength of the end ring and adjacent flanged collar combined.

Pre-forming of the continuous channel spacer from flat pressboard strip is illustrated in Figure 10. The automatic machine shown was designed especially for this purpose and normally produces channel spacer at the rate of one hundred feet per minute without an attendant. The advantages of this continuous spacer forming process from a production viewpoint are obvious, particularly when compared with the manufacture of a vast number of intermittent radial spacers which are individually sheared, punched, and assembled.

Figure 11 shows a partially wound high-voltage coil and indicates the manner in which the conductors and encasing channel spacer are simultaneously wound into the coil. It is evident that the winding process is substantially continuous, and the delays necessarily associated with intermittent spacers are eliminated.

Progressive assembly of the individual high-voltage coils as independent units is indicated by Figure 5, which shows a tap coil being lowered into a partially built structure.

VII. Summary

More than one-quarter million kilovolt-ampere capacity of the distributed concentric-type transformer has been manufactured and placed in service up to the present time. Figure 12 indicates the general external appearance of a typical core and coil assembly and illustrates the cylindrical shields, end-insulation structure, and horizontal coil crossovers.

Outstanding characteristics of the distributed-concentric-type design include:

1. Reduced inherent reactance, resulting in a reduction of approximately 10 per cent in total weight of critical materials required for a given transformer specification.

2. Ready adaptability to forced oil flow directed through the windings, resulting in an increase of approximately 20 per cent in thermal capacity of a given transformer and external cooler, or a reduction of approximately 15 per cent in total weight of critical materials required for a given kilovolt-ampere output.

3. Strategic distribution of low frequency and impulse voltage stresses throughout the whole dielectric structure, including a reduction of approximately 70 per cent in the voltage between shield and winding, the advantageous distribution of all voltage stresses resulting from the physical distribution of the high-voltage winding *through* the major insulation.

4. Simple and stable mechanical construction of high-voltage winding, insulation, and shield, including a new and improved form of helical coil structure and associated assembly which are mechanically suitable for application in large high-voltage power transformers.

References

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2. Figure 1 drawn from Canadian patent number 304,178. Inventor, **K. K. Paluev.**

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Experience With Oil Circuit Reclosers on REA Systems

LEE M. MOORE
MEMBER AIEE

BRUCE O. WATKINS
ASSOCIATE AIEE

Synopsis: For three years surveys have been made of the operation of small single-pole automatic reclosing oil circuit breakers (also called "oil circuit reclosers" and "service restorers"[1]) on rural electric distribution systems financed by the Rural Electrification Administration. Results of these surveys indicate that the ratio of lockouts to operations is less than five per cent in most cases, and that there are from four to nine operations per month per breaker during the spring and summer, with each breaker controlling about 20 miles of line. The number of breaker failures and other breaker difficulties have been a small percentage of the total number of breakers installed. These surveys, together with discussions with system managers, have indicated that automatic reclosing breakers offer many advantages for rural line sectionalizing. Presently available breakers can be much improved in operating characteristics, and new sectionalizing devices are needed. This paper presents suggestions for such improvements.

THERE are now over 800 energized rural electric distribution systems financed by the Rural Electrification Administration, and these systems serve over 1,000,000 consumers. The average number of miles per system is about 470, and usually these lines radiate out of and are fed from a single supply substation. These rural lines extend out 40, 50, and sometimes over 60 line-miles from the power source. In general, the systems follow the radial design. In a few cases alternate feeders extend into the same area, but normally these feeders are not interconnected. The main feeders, two or more in number, consist of three-phase four-wire multigrounded neutral wye circuits with a nominal voltage of 7,200/12,450. Circuits, with two-phase wires and neutral, branch off from these feeders, and these "vee" circuits again split into single-phase circuits with one-phase wire and multigrounded neutral. Approximately 80 per cent of all distribution lines are single-phase 7,200 volts, and the greatest percentage of the consumer load is single phase. Outdoor unattended substations are used principally.

Individual systems will of course vary in detail from the average. Some are smaller and some larger. The larger systems usually divide the area into sections serviced by submaintenance centers. In the western mountainous regions the areas served are usually long and slender, and the lines follow the valleys. In these areas and in hilly regions, the

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Lee M. Moore is with the Rural Electrification Administration, U. S. Department of Agriculture, St. Louis, Mo. as principal engineer in the technical standards division. Bruce O. Watkins formerly of the Rural Electrification Administration, is now in active duty in the United States Navy.

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road distance to outlying sections may be much greater than the line distance.

Although progress has been made in improving rural roads, in many sections of the country road conditions leave much to be desired. During wet weather some of these rural roads become impassable.

Satisfactory telephone communication facilities generally exist in the more thickly populated farming areas, but in the more sparsely settled areas communication is sometimes poor and frequently practically nonexistent.

Table I. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems in Iowa From Installation Until November 1, 1940

Number of systems reporting	20
Total number of circuit breakers reported	417
Number of design 1 breaker	163
Number of design 2 breaker	254
Total reported circuit-breaker months of operation	5,098
Total number of operations reported	20,973
Total number of lockouts reported where operations were reported	271
Per cent of lockouts to operations	1.29
Total operations where operations and number of breakers were both reported	20,973
Total number of breakers where operations and number of breakers were both reported	301
Average number of operations per month per breaker	4.11
Total reported breaker failures	59
Percentage of design 1 breaker failures per 12 months, where failures and months of operation were both reported	21.8
Percentage of design 2 breaker failures per 12 months where failures and months of operation were both reported	2.04

Most systems are equipped with open-gap-protected transformers.

All of the preceding information furnishes a picture to haunt the dreams of an operating man responsible for the continuity of service consistent with good operating practice. Rural service must be good. Farmers, using electricity for food-production purposes, need and are entitled to better service than urban resident consumers. In order to provide means for improving rural service continuity, the development of carrier-current communication, hot line work, routine testing, and so forth, for use on systems described, has been carried on. A heavy share of the responsibility for continuous service, however, must be borne by the sectionalizing devices. These must isolate trouble properly with a minimum of attention, and REA has been very interested in adequate sectionalizing apparatus and methods.

In the early stages of the REA program, most systems were designed to be sectionalized with fuses. Three-shot repeating cut-outs were installed on main feeders, and single-shot cut-outs served on branch lines. Few substations were equipped with oil circuit breakers; three-shot cut-outs served at these locations. Since then, the fused system has been used very extensively, but, since experience has shown that many unnecessary outages occurred with the single-shot cut-out, the trend has been toward using fewer single-shot cut-outs and using more of the repeating type.

In 1936, 7,500-volt automatic reclosing and recycling single-pole circuit breakers were available, which were recommended for use on circuits up to 7,500 volts. These were installed on single-phase lines on a number of rural systems in conjunction with open-gap-protected distribution transformers. One object of these installations was to reduce construction costs by eliminating lightning arresters, and another object was to determine if such a device would be advantageous in decreasing service interruptions. In particular, many Iowa systems and some Ohio systems used this construction.

Experience with the 7,500-volt breaker installations proved that a device which would automatically recycle to original position after a temporary fault and would reclose two or three times, then lock out on a permanent fault, would do much to reduce system outages. However, because of the low insulation level of the device, a considerable number of breaker failures were experienced, most of these from lightning surges (see Table I). Because of these difficulties, use of this device was largely abandoned.

About 1939, stimulated by REA engineers and others, a 15-kv single-pole self-contained automatic reclosing oil circuit breaker was developed. The breaker opening time was very short (usually less than 0.1 second), and the time between all three reclosures was about three seconds and was unadjustable. Systems along the eastern seaboard decided to try out this device. In about a year's time 1,000 such circuit breakers had been installed. Each of these systems was separately studied for proper application of these devices.[2] Following a thorough study, taking into consideration the proper application of these breakers from the viewpoint of safety, accessibility,

Table II. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems, May to November 1941

Month	Number of States Represented	Number of Systems Reporting	Number of Breakers Reported	Average Miles of Line Per Breaker	Operations Per Breaker	Operations Per Mile	Per Cent Lockouts to Operations
Systems With Arrester-Protected Transformers							
May	7	10	62	31.3	6.01	0.192	1.88
June	10	17	192	24.2	9.07	0.375	2.53
July	15	28	323	27.8	9.69	0.349	1.73
August	15	35	405	31.9	7.93	0.249	2.71
September	18	36	368	32.2	2.85	0.089	3.63
October	13	18	189	30.7	2.69	0.088	13.4*
Systems With Open-Gap-Protected Transformers							
May	7	23	428	18.4	4.24	0.230	2.70
June	10	31	580	18.0	5.97	0.332	2.31
July	11	41	662	18.9	7.60	0.402	1.73
August	13	46	684	19.7	6.70	0.340	1.55
September	15	47	697	19.5	6.50	0.333	2.50
October	12	20	336	18.6	5.37	0.289	5.09

* Due to a large number of trees breaking on one system.

Table III. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems Which Reported the Same Number of Breakers for the Three Consecutive Months of July, August, and September 1941

	All Systems	Systems With Gap-Protected Transformers	Systems With Arrester-Protected Transformers
Number of states represented	17	9	14
Number of systems reporting	35	18	21
Number of breakers reported	450	204	246
Miles of line controlled	11,441	4,150	7,291
Average miles of line per breaker	25.4	20.4	29.6
Number of breaker operations	8,094	3,228	4,866
Total operations per breaker	18.0	15.8	19.8
Total operations per month per breaker	6.0	5.3	6.6
Total operations per mile	0.706	0.778	0.668
Total operations per month per mile	0.235	0.259	0.223
Number of breaker lockouts	208	77	131
Lockouts per month per breaker	0.154	0.126	0.177
Lockouts per month per mile	0.00607	0.0062	0.00598
Per cent lockouts of operations	2.57	2.38	2.69
Operations per lockout	38.9	41.9	37.1

co-ordination, and cost, a definite program was recommended for breaker installations so as to attain the maximum benefit from their use. This study indicated that the breaker investment would pay greater dividends when the device was located at the more remote sectionalizing points, and hence the first breakers were installed at a distance from the power source, and as additional breakers were obtained, installations were made progressively nearer to the power source. No discrimination was made as to the type of transformer protection. Existing cut-outs were left in place at sectionalizing points on the load side of the breakers and used as disconnects. Experience with these circuit breakers has been so favorable that since then many additional systems have installed such devices and REA-financed systems now have over 5,500 such single-pole automatic circuit breakers.

The first detailed survey of circuit-breaker performance was made in 1940, when the Iowa systems were asked to give their experience from the date of installation to November 1, 1940. Unfortunately, most of the systems kept no adequate records of some phases of such breaker operation, and the investigation was made more difficult by the fact that the counters of the older 7,500-volt breakers were internally placed. However, out of the replies furnished, the most adequate data were assembled as shown in Table I. It is remarkable how closely such data check with the more accurate information obtained during the next two years.

Most of the failures on the design 1 breaker were due to lightning damage to the pocket-type bushing and insulated lead. The next greatest failure cause was overload, which in turn was due to incorrect application. The design 2 breakers showed a very much lower failure rate.

In order to obtain more detailed and accurate data and to cover more cases, during the 1941 lightning season all REA-financed systems with circuit breakers were asked to make breaker counter readings each month and to report the number of lockouts and the cause of each. Tables II through V give the results of this survey. (The term "Gap" means an open air gap of definite dimensions which has no means of extinguishing the flow of power follow current.)

Systems which reported the same number of breakers for the three consecutive months of July, August, and September were segregated, and Table III gives the breakdown or analysis of data for these systems.

Table IV shows the cause of all the reported lockouts, irrespective of month or system, and Table V shows the breaker difficulties experienced.

Since any failures or other difficulties with breakers were reported separately, and since systems which did not furnish operation and lockout data often did report such difficulties, the tabulation in Table V represents many more breaker installations than reported in the other tables and covers the entire year. Hence, the percentage of failures to installations could not be determined, although apparently it is very low.

During 1942, because of the gasoline and tire shortage, REA-financed systems were asked to make complete counter readings only on April 1 and on October 1 but to make lockout reports each month. In 1941 two additional manufacturers had breakers available, and some systems with these makes were included in this survey. These new makes have different characteristics and construction from the older makes, but in general the operation is the same. Table VI gives a summary of the data collected in the 1942 survey.

In the 1942 survey, lockouts were reported each month, and, if no report was obtained after a letter follow-up, it was assumed that no lockouts occurred. Hence, some lockouts may have been omitted, accounting for the lower percentage of lockouts to operations than in 1941.

Figure 1. Estimates of average cost to re-fuse one cutout by 132 REA-financed systems with 1,337 breakers. January 1 to October 1, 1942

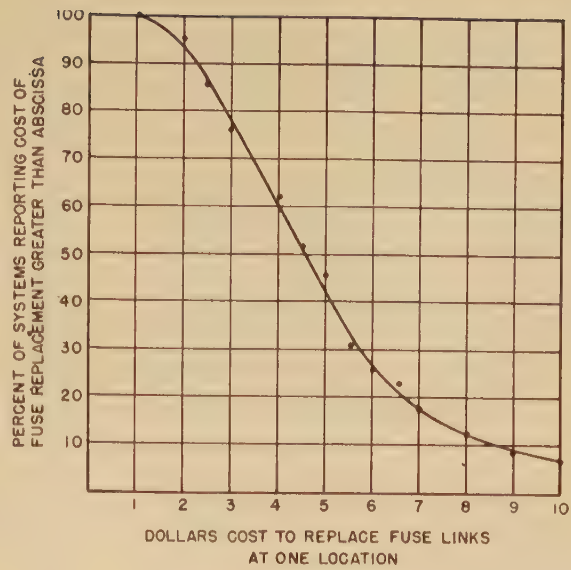


Table IV. Report of Single-Pole Reclosing-Circuit-Breaker Operations on REA-Financed Systems, May to November 1941

Cause of Breaker Lockouts

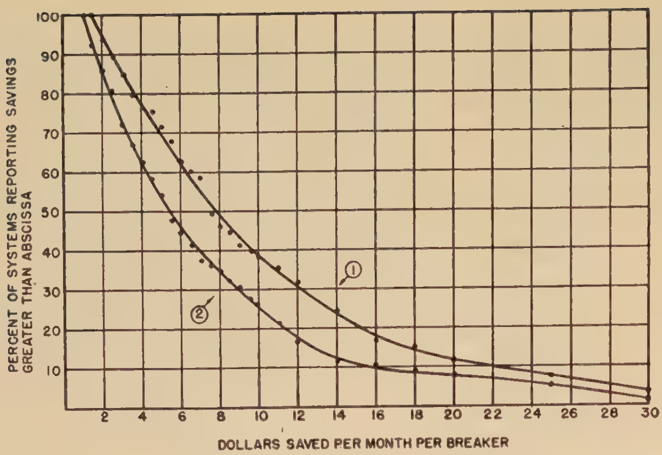
Number of states reporting	20
Number of systems reporting	72
Total lockouts reported	773
Percentage lockouts due to:	
1. Conductor break	7.3
2. Pole break	3.1
3. Insulator failure	7.5
4. Arrester failure	4.5
5. Transformer failure	9.8
6. Trees	30.7
7. Other	21.1
8. Not known	16.0

Table V. Report of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems

Reported Difficulties Experienced With Breakers—1941

State	Number of Breakers Affected	Description of Difficulties	Probable Cause of Difficulties
Florida	5	Oil gauges did not work	Fiber gauge rod injured in shipment
	1	Breaker jacket injured	Lightning
Georgia	2	Did not reclose	Rust on trip counters
Iowa	2	Gears stripped on recycling device	Breakers improperly located on system
	1	Coil lightning protector failed	Lightning
	1	Failure to operate	Not known
Minnesota	4	Failure to operate	Lightning*
North Carolina	1	"Burned out"	Not known
South Carolina	2	Registers did not operate	Not known
Texas	1	Coil protector failed—breaker case shattered	Lightning
Wisconsin	1	Breaker case shattered	Not known
Total	21		

* These are an older model.



Curve 2 shows estimates of savings in operation and maintenance costs by 100 REA-financed systems with 1,078 breakers. Curve 1 shows estimates in savings in operation and maintenance plus savings in retail sales by 53 REA-financed systems with 507 breakers. January 1 to October 1, 1942

Figure 2. Estimates of monetary savings using breakers over previous practice

In view of the fact that in the field there occasionally seems to be no clear-cut distinction between a gap and a lightning arrester, a few errors may exist in the 1941 and the 1942 surveys in separating the systems with gap-protected transformers and the systems with arrester-protected transformers. However, errors from this cause are small and should have little effect in the over-all results as shown on the summary for the period covered. The surveys indicate that there were about 26 per cent more operations of the breaker per mile of line with gaps than with arresters in 1942 and about 16 per cent more in 1941. Lightning protection is provided only at each distribution transformer, or on about 20 per cent of the poles on rural circuits. Each unprotected pole provides a potential point of flashover, and each such flashover will probably cause an operation of the breaker. Since the arrester will usually stop follow current without breaker operation, while the gap will not, the additional operations per mile with gap-protected transformers can be expected.

Table VII shows the reported cause of all reported 1942 lockouts. Table VIII gives a summary of the breaker difficulties experienced. As in 1941, Table VIII represents more breaker installations than reported in the other tables, and covers the entire year.

Records for circuit-breaker operation have not been obtained for winter months. Experience has shown that during this period the number of operations reduces considerably. Numerous letters and reports from system managers indicate that the oil circuit breaker is very effective in preventing outages caused by unequal snow loading and tree limb contacts. Particular attention is called to the fact that these systems have been operating with reduced personnel under national defense and wartime conditions. Because of this situation an interesting aspect of the use of the circuit breaker which showed up on some systems was that tree trimming and right-of-way clearing were often deferred if other work was pressing, allowing the breakers to take care of the situation.

In the 1942 survey an attempt was also made to ascertain the monetary savings experienced by REA-financed systems using breakers. Systems were asked to furnish estimates on such savings. Figures 1 through 3 give the results of this survey. Figure 1 shows a percentage distribution of estimated savings for replacing fuses in a cut-out at one location. Experience has shown that circuit breakers usually show a greatly higher operation rate than the previous rate of fuse blowings at the same location. This may be caused by breaker operations occurring during the time which was formerly taken to replace the blown fuses. It is therefore not proper to multiply the number of breaker operations by the cost to replace fuses at a given location in order to obtain the total savings. Hence, the systems were asked to give estimates on the monthly savings effected by breakers, and these are shown in Figure 2. Figure 3 shows the estimated breaker maintenance and replacement cost. These three charts are for the period of January 1 to October 1, 1942. Maintenance costs on the breakers, now low, will probably increase as the units become older.

Correspondence and discussions with various REA system managers indicate that the continuity of service brought about by the breakers is of far more importance than the savings in maintenance and operations expenses afforded by them. It is difficult to get at any

Table VII. Report on Operation of Reclosing Oil Circuit Breakers on REA-Financed Systems April 1 to October 1, 1942

Cause of Lockouts

Number of states reporting	31
Number of systems reporting	163
Total lockouts reported	1,930
Percentage of lockouts due to:	
1. Conductor break	10.1
2. Pole break	1.9
3. Insulator failure	11.8
4. Arrester failure	5.4
5. Transformer failure	5.6
6. Trees	25.5
7. Other	18.5
8. Not known	21.2

Table VI. Summary of Survey of Single-Pole Reclosing-Circuit-Breaker Operation on REA-Financed Systems, April 1 to October 1, 1942

	All Systems	Systems With Gap-Protected Transformers	Systems With Arrester-Protected Transformers
Number of states represented	31	23	29
Number of systems reporting	162	88	99
Number of breakers reported	2,455	1,314	1,141
Miles of line controlled	55,238	26,680	28,558
Average miles of line per breaker	22.6	20.3	25.0
Number of breaker operations	98,858	53,714	45,144
Total operations per breaker	40.3	40.8	39.5
Total operations per month per breaker	6.7	6.8	6.6
Total operations per mile	1.79	2.01	1.58
Total operations per month per mile	0.298	0.335	0.264
Number ot breaker lockouts	1,840	877	963
Lockouts per month per breaker	0.125	0.111	0.141
Lockouts per month per mile	0.00555	0.00548	0.00562
Per cent lockouts of operations	1.87	1.63	2.14
Operations per lockout	53.7	61.2	46.8
Per cent consumer hours outage time	0.105	0.097	0.111

Table VIII. Report on Survey of Single-Pole Reclosing Circuit Breakers on REA-Financed Systems, 1942

Summary of Reported Difficulties

Probable Cause of Difficulties	Number of Breakers
Lightning	13
Defective or broken ratchet mechanism	7
Warped or defective lockout mechanism	8
Overload	3
Burned contacts or worn parts	4
Dirty	3
Excessive or insufficient fault current	4
Noninterruption of fault	1
Other	3
Not known	10
Total	56

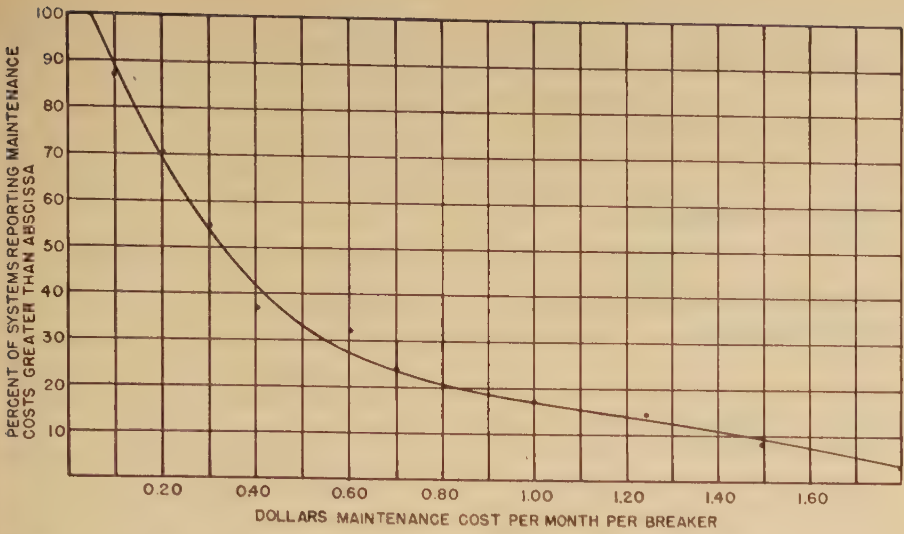


Figure 3. Estimates of breaker maintenance costs by 47 REA-financed systems with 624 breakers, January 1 to October 1, 1942

figures on the number of consumer-hours outage time saved by the breaker system over the fused system, as few of the fused systems kept any detailed records of outage time. Indications are, however, that the improvement in most cases is substantial.

These surveys and other experience have indicated that the automatic reclosing oil circuit breaker offers great possibilities for rural electric system sectionalizing. Since most of the REA lines are single phase, the greatest requirement is for single-pole breakers. Some systems, particularly those serving a large amount of irrigation load, use automatic reclosing three-pole breakers. In order to prevent voltage from appearing on an opened phase through ungrounded wye transformer banks and also to prevent single-phase power being applied to three-phase motors, it appears that three-pole breakers on feeders may become more necessary as the many small industries continue to spring up around established REA systems requiring more and more three-phase power.

The circuit breaker will probably not entirely replace the fused cut-out immediately or even in the future. In some locations, where the routine patrolling is of sufficient frequency, the fused cut-out may be more economical and the service maintained as satisfactorily as with the automatic breaker. Also, if a breaker such as described hereinafter can be obtained, single-shot fuse cut-outs can be used on short branch lines beyond such breakers.

One of the major questions in the future development of the small distribution-type circuit breaker is the question of time delay in opening. From the standpoint of protection to equipment and prevention of conductor failures caused by arc-overs, the breaker should open as rapidly as possible. On the other hand, one of the objections to the fast-opening breaker has been that occasionally a damaged distribution transformer takes a line section out of service because the breaker locks out prior to the blowing of the primary transformer fuse. Lockouts from this cause seldom occur, but, when they do, a large number of consumers may experience a long outage. Also, it is believed that time delay should afford an opportunity to co-ordinate a greater number of breakers in series and should provide better operation when picking up motor loads.

Limitations are often imposed by the power supply agency, which usually specifies the supply side fuse size on the substation. If the purpose of this limitation is to insure co-ordination with relays further back on the transmission system, it can be understood, but unfortunately in many cases the supply side fuse size seems to be specified on no particular technical basis. Proper sectionalizing depends on system fault currents and time-current characteristics of apparatus, and the supply side fuse or other device is intended to operate only for short circuit in the substation or in case the load side device fails. As pointed out by Marsh and Dodds,[3] it is not always possible to expect complete co-ordination of the substation fuse with transmission-line relays over the entire range of fault currents and still allow adequate sectionalizing by the consumer. In particular instances, discussion of the problem with supply agencies has usually resulted in a revision of requirements, and it is hoped that eventually all supply agencies will co-operate with consumers in reaching a solution satisfactory to both parties.

In any case, the protection of equipment and the increasing use of high-speed relays puts a ceiling on the amount of time delay used, particularly since the breaker reopens three or four times for a lockout. Present indications are that the device should have fast opening on the first one or two openings and moderate time delay on succeeding openings. This would effect a compromise and allow the use of single-shot fused cut-outs beyond the breakers on short branch lines.

A possible specification for a small distribution circuit breaker would have it satisfy the following requirements:

(*a*). The breaker be rugged and dependable.

(*b*). Be self-contained, requiring no current or voltage transformers.

(*c*). Be entirely automatic.

(*d*). Be equipped with two instantaneous openings and two subsequent time-delay openings to lockout, with recycling to original position for a temporary fault.

(*e*). Contain lightning and overload protection.

(*f*). Have a high insulation level.

(*g*). Have a fairly simple mechanism, easy to repair.

(*h*). Be inexpensive.

Future developments in rural electrification point to increased use of three-phase lines and interconnection of lines into networks. This trend will call for some sort of directional control on breakers. Devices which will distinguish between load and fault currents of the same magnitude are required. There is much to be done in the rural electric distribution field, particularly in circuit control.

References

1. Interim Report on Application and Operation of Automatic Reclosing Equipment on Stub Feeders, AIEE subcommittee on a-c automatic reclosing equipment. AIEE Transactions, volume 62, 1943, August section, pages 536–40.

2. Procedure for Making a Sectionalizing Study on Rural Electric Systems. *Technical Standards Bulletin* 4, United States Department of Agriculture, Rural Electrification Administration.

3. High-Voltage Fusing of Transformer Banks, H. H. Marsh, Jr., G. B. Dodds. AIEE Transactions, volume 61, 1942, July section, pages 533–5.

Interim Report on Application and Operation of Automatic Reclosing Equipment on Stub Feeders

AIEE COMMITTEE ON AUTOMATIC STATIONS
Subcommittee on A-C Automatic Reclosing Equipment

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports in this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the Standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment without changing the fundamental basis on which the present standards have been prepared.

Synopsis: This report presents the results of a survey which has been made to obtain data on typical present-day a-c automatic reclosing equipment and practices as applied to stub feeders having a single end source of power. The data received provide a good summary of the reclosing practices in general use and the performance being obtained. It also gives an indication of the possibilities of using immediate initial reclosure more extensively for some types of loads. This subject is of timely interest because of the possibilities of conserving critical materials in such cases where the application of automatic reclosing equipment will defer the necessity of providing additional circuit facilities.

Paper 43-84, recommended by the AIEE committee on automatic stations for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 7, 1943; made available for printing May 10, 1943.

Personnel of the subcommittee on a-c automatic reclosing equipment: J. A. Elzi, *chairman;* J. T. Logan, G. S. Whitlow.

This report was prepared by the AIEE subcommittee on a-c automatic reclosing equipment of the committee on automatic stations for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard" and has not been approved formally by the Standards committee or the board of directors.

It will be noted in the tabulation that 41 users furnished the data summarized in this report. The committee on automatic stations expresses its sincere appreciation to these organizations for their co-operation.

THE value of the use of automatic reclosing equipment is generally recognized, but there is quite a bit of latitude possible in the application of equipment and the extent to which the greatest benefit will be derived from it. Operating experience plays an important part in determining the most suitable type of equipment and the optimum settings.

The scope of this survey was limited to stub feeders which are energized from one source only and which do not require voltage-checking devices nor synchronizing relays.

The data presented in this report are based on information supplied by 41 users and represent a total of over 100 typical applications covering a little more than 70,000 miles of circuits in use by various companies or governmental units in the United States and Canada. The operating voltages range from 2,300 volts and above, and the data submitted cover both isolated and grounded neutral systems. A summary of the data regarding types of equipment used, reclosing cycles, and performance is given in Table I.

Reclosing Cycle

In analyzing the data regarding the reclosing cycle most commonly used, it was felt that it would be most significant to study the information on the basis of the interval of time used for the first reclosure and to apply a weighting factor proportional to the circuit miles for which any particular initial reclosing cycle is used. The time interval used for the second and third reclosures varies rather widely and depends on such factors as allowable breaker duty, relaying requirements, and allowance for clearing of temporary faults caused by tree limbs and swinging conductors. An analysis of the data received made on the aforementioned basis is shown in Figure 1. Immediate initial reclosing was reported for 70 per cent of the circuit miles for all types of loads. For circuits supplying 75 per cent or more residential load, immediate initial reclosing is used for 88 per cent of the circuit miles, while for circuits supplying loads comprised of 25 per cent or more motor load, immediate initial reclosing is used for only 30 per cent of the circuit miles and an initial interval of 15 seconds or more is used for 68 per cent of the circuit miles.

Successful Reclosing Operations

The results of the questionnaire indicate that most users do not provide a means of differentiating between successful initial, second, or third reclosures. They appear to have definite data as to the per cent of successful reclosures and the per cent of lockouts, but in most cases have estimated the number of successful reclosures which were second and third intervals. Figure 2 has been prepared to show the successful initial reclosing performance in terms of circuit miles, and Figure 3 gives the average for all companies weighted in proportion to the circuit miles. From Figure 2 it will be seen that 80 to 90 per cent successful initial reclosing performance was reported for 53 per cent of the circuit miles and 70 to 80 per cent successful reclosure for 34 per cent of the circuit miles. Figure 3 indicates that service is restored on the initial reclosure for 76 per cent of the circuit miles.

Change of Relaying Time After Initial Reclosure

For about 75 per cent of the types of circuits reported, no provision is made for

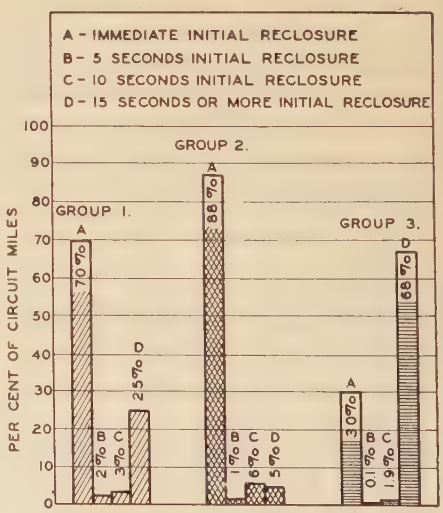


Figure 1. Summary of initial reclosing intervals

Group 1. Average for all types of loads

Group 2. Average for circuits having 75 per cent or more residential load

Group 3. Average for circuits having 25 per cent or more motor load

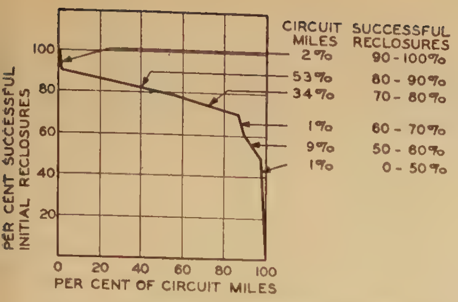


Figure 2. Summary of successful initial reclosures

changing the relaying time after the initial reclosure which would permit the circuit breaker or service restorer to clear the fault initially, providing selectivity with fuses or other fault clearing devices for subsequent reclosures. Of the 25 per cent of the users who do use this type of relaying, several have kept quite complete records and appear to be obtaining satisfactory results. Various schemes of providing this type of relaying were described. Some companies make use of a contact on the reclosing relay, others provide separate plunger-type relays, and in some cases this feature is incorporated in the service restorer.

Available Reclosing Relays

A large majority of the users reported that the present available reclosing relays are satisfactory for their systems. There was an expression on the part of some

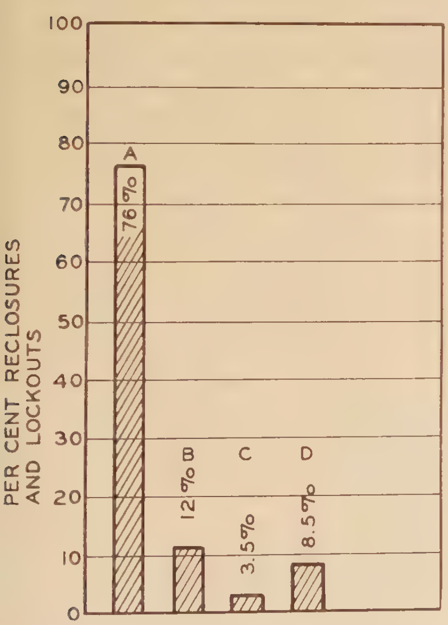


Figure 3. Summary of successful initial reclosures

A. Successful initial reclosures (including immediate and time delay)
B. Successful second reclosures
C. Successful third reclosures
D. Lockouts

users that it would be desirable to have provision made for a quicker reset time on reclosures so as to avoid lockouts on recurring faults.

Available Oil Circuit Reclosers and Service Restorers

Quite a few of the users did not have this type of equipment in service or had insufficient experience to comment on this question. Of those that do use this equipment, approximately 50 per cent express the opinion that the present equipment is satisfactory and adequate, while the remainder indicated desired modifications. The features most frequently mentioned as being required are as follows:

(*a*). Greater interrupting capacity.

(*b*). Better time-delay features to permit co-ordination with fuses and other fault-clearing devices.

(*c*). Higher continuous current ratings.

Most of the users indicated a decided preference for three-pole devices when used on three-phase circuits and single-pole devices on single-phase circuits.

Repeater Fuses

Some 90 per cent of the operators using repeater fuses do not seem to find that the lack of the self-resetting feature results in excessive lockouts. Most of the users report that customary patrols of circuits are adequate to prevent excessive lockouts from this cause and do not indicate an excessive amount of patrolling being required to obtain this performance.

Maximum Time of Circuit Interruption Without Dropping Motor Load

Only a relatively few users have factual data regarding the maximum time a distribution circuit can be de-energized without dropping an appreciable part of motor load. However, those who did present data appear to have analyzed the situation rather carefully, and it is felt that the information given is therefore quite significant. Figures of 0.5 to 2 seconds appear the most frequently. One user reports the results of tests which indicate that for 98 per cent of the motors tested, an interruption of from 1 to 2.2 seconds would not result in dropping the induction motor load.

There is quite a divergence of practice in the application of immediate reclosure for motor loads. Some users have adopted the practice of providing no immediate reclosure for any motor loads, but many report satisfactory performance with immediate reclosure for induction motors, and a few use this type of reclosure for synchronous motors as well. In this connection there are several considerations mentioned which are worthy of note:

(*a*). For the successful application of immediate reclosure for motor loads, special consideration may need to be given to the type of load served and the product being manufactured.

(*b*). Time delay is provided for undervoltage release devices where used.

(*c*). For some synchronous motor loads it may be necessary to provide suitable equipment such as field removal relays, unloading devices, and so forth, to insure satisfactory operation.

The replies to this part of the questionnaire indicate that there is a need for special study in applying reclosing equipment to some motor loads, especially synchronous motors and that there is a definite possibility of utilizing the benefits of immediate reclosure on motor circuits to a much wider extent than is now being done.

Minimum Permissible Reclosing Cycles

Here again it was found that there is a dearth of factual data regarding the minimum time required between reclosures to assure deionization of the arc path. Many of the users reporting on this part of the questionnaire expressed the opinion that an interval of approximately 0.5 second is satisfactory, while it was pointed out also by several that the time interval obtained by the use of normal speed breakers and immediate reclosing relays has resulted in no trouble because of restriking of the arc. Some do, however, feel that a period of 3 to 5 seconds should be allowed to permit swinging conductors to separate and falling tree branches to clear from the lines before they are re-energized. Other users rely on the second reclosing interval to take care of these types of faults.

Co-ordination Between Reclosing Equipment and Fuses

Many of the users who replied to the questions regarding experience in obtaining successful co-ordination between reclosing equipment and fuses or reclosers and fuses report satisfactory results if care is taken in making the co-ordination study before the equipment is applied.

Table I. Summary of Equipment and Performance Data

Company Number	Service Voltage	Number of Wires	Connection	Grounding	Open Wire (Per Cent)	Cable (Per Cent)	Circuit-Miles	Load: Residential	Load: Induction Motor	Load: Synchronous Motor	Equipment: Type	Poles	Reclosing Cycles (Seconds)	Initial	Second	Third	Lockout	Most Common Cause of Outage
1	4,160	4	Wye	Multi	95	5	—	90	10	—	Service restorer	3	—	90	—	—	—	—
	12,000	3, 4	Delta, Wye	Multi	95	5	—	90	10	—	Oil circuit breaker	3	15–30 urban 30–60 suburban	90	—	—	—	—
2	16,000	3	Wye	Uni	100	—	—	50	50	—	Oil circuit breaker	3	15–30–45	—	—	—	—	Wind
	11,000	3	Wye	Uni							Service restorer	3	—					
	7,000	4	Delta	Uni							Oil circuit recloser	3	—					
	4,000	4	Wye	Multi	Sensitive ground protection on some 7-kv, 4-kv, 11-kv isolated neutral circuits.													
	2,400	3	Delta	Uni														
3	13,000	3	Delta	Uni	100	—	5.8	10	60	30	Oil circuit breaker	3	Instant	100	—	—	—	Lightning
	13,000	3	Delta	Uni	100	—	9.5	40	60	—	3-shot fuses Oil circuit breaker	3	5–65–60	100	—	—	—	—
	13,000	3	Delta	Uni	100	—	33.3	80	20	—	Oil circuit breaker	3	—	60	20	—	20	—
	4,000	4	Wye	Multi	100	—	52.7	—	—	—	Oil circuit breaker	3	50–30–95	80	—	—	20	Wind and trees
	4,000	4	Wye	Multi	100	—	196.8	—	—	—	2-shot fuses 3-shot fuses	—	— —	83 100			17 0	
	2,300	3		Multi	100	—	7.9	25	75	—	Oil circuit breaker	2	15–30–30–60	—	—	—	100	
4	13,800	3	Delta	Uni	100	—	11	75	25	—	2-shot fuses		—	50	40	—	10	Lightning
	13,800	3	Delta	Uni	100	—	?	80	20	—	3-shot fuses		—	50	20	20	10	Lightning
	13,800	3	Delta	Uni	100	—	3	90	10	—	3-shot fuses		—	70	10	10	10	Lightning
	13,800	3	Delta	Uni	100	—	112	75	25	—	Oil circuit breaker	3	—	58	21	8	13	Lightning
	13,800	3	Delta	Uni	100	—	98	75	25	—	Service restorer	3	10–10–30	64	14	5	17	Lightning
	13,800	2	1-phase	Uni	100	—	11	95	5	—	3-shot fuses		—	50	20	20	10	Lightning
	2,300	3	Delta	Iso	100	—	9	60	40	—	Oil circuit breaker	3	15–15–30	90	0	0	10	Lightning
	2,300	3	Delta	Iso	100	—	21.5	80	20	—	Oil circuit breaker	3	15–30–75	25	25	25	25	Lightning
5	33,000	4	Wye	Uni	—	—	—	—	—	—	Oil circuit breaker Oil circuit reclosers	3 3	1st 0–18 2nd 20–52	—	—	—	—	Lightning
	4,500	4	Wye	Multi	90	10	—	—	—	—	Oil circuit breaker	1	Instant 0–30–90 5–30–90 5–30–125	—	—	—	—	Lightning
					Also use one-pole and some three-pole oil circuit reclosers													
6	7,200	2	Wye	Multi	100	—	35	—	—	—	3-shot fuses		—	—	—	—	14.8	
	7,200	2	Wye	Multi	100	—	204	—	—	—	Oil circuit reclosers	1	—	—	—	—	12.5	
	4,400	4	Wye	Uni	95	5	1,192	50	50	—	Oil circuit breaker	1	15–30–75	72	7.9	1.4	18.7	—
				Total for 84 circuits—7 per cent of circuits have instant reclosure.														
7	11,950	4	Wye	Uni	100	—	79	88	12	—	Oil circuit recloser	1		95			5	—
	11,950	4	Wye	Uni	100	—	1,388	88	12	—	Oil circuit breaker multigrounded in 1938–1942	1	0–45–75	76.3	7.4	1.4	14.9	Lightning, trees
				Total of 23 circuits of 12-kv.														
	33,000	3	Wye	Uni	100	—	263	60	30	10	Oil circuit breaker	3	0–45–75	92.4	2.3	0.6	4.7	Lightning
	11,950	4	Wye	Uni	100	—	150	95	5	—	3-shot fuses		—	55.6	11.1	—	33.3	—
				Nine 12-kv circuits, unigrounded in 1938, multigrounded by 1942.														
8	13,200	4	Wye	Multi	99.6	0.4	245	55	40	5	Oil circuit breaker	3	0 — —	90.1	4.0	—	5.9	Lightning
	4,160	4	Wye	Multi	99.4	0.6	360	74	25	1	3-shot fuses Oil circuit breaker	3	0 — —	67.6	2.7	8.1	21.6	Lightning
9	All	3, 4	Delta, Wye	All	100	—	5,000	—	—	—	Oil circuit breaker	3	0–15–75	84	10	1	5	Lightning
											Three-pole service restorers and 1-pole oil circuit reclosers used.							
10	22,000	3	Delta	Isolated	100	—	73	80	20	—	Oil circuit breaker	3	0–30–60	69	4	4	23	Lightning
	11,000	3	Wye	?	100	—	38	87	13	—	Oil circuit breaker	3	0–60–60	53	10	1	36	Lightning
	44,000	3	Delta	Isolated	100	—	62	75	25	—	Oil circuit breaker	3	0–60–60	43	52	2.5	2.5	Lightning
11	4,160	4	Wye	Multi	100	—	—	80	20	—	Oil circuit breaker	3	6–15–75	—	—	—	—	—
					Also use 3-shot fuses, 1-pole service restorers, and 3-pole oil reclosers.													
12	4,150	4	Wye	Multi	100	—	300	95	4	1	Oil circuit breaker	3	0–20	61	11	—	38	Wild fowl
											(For many old oil circuit breakers	3	17–20)	Also use 3-pole service restorers.				
13	2,300	3	Delta	Isolated	100	—	—	—	—	—	Oil circuit breaker	3	15–30–75	—	—	—	—	Lightning
	11,950	4	Wye	Multi	100	—	—	—	—	—	Oil circuit breaker	3	0–30–75	Approximately 75–90 per cent instantaneously successful.				
					Three-pole oil circuit reclosers used on 12 kv.													
14	44,000	4	Wye	Multi	80	20	—	30	70	5	Oil circuit breaker	3	10	—	—	—	—	Lightning
15	2,300 11,000	3 4	Delta Wye	Isolated Multi	100	—	—	67	33	—	Oil circuit breaker	3	1–30–90	—	—	—	—	Lightning
16	110,000	3	Wye	Uni	100	—	66	—	—	—	Oil circuit breaker	3	2	93.6	—	—	6.4	Lightning
	26,400	3	Wye	Uni	100	—	41	—	—	—	Oil circuit breaker	3	1.7	71	—	—	29	Lightning
	8,000	4	Wye	Uni	100	—	190	90	10	—	3-shot fuses		—	23	32	—	45	Lightning
					Two 40-ampere and one 75-ampere links used at station with 50-ampere and 60-ampere branch fuses.													
17	13,200	3	Wye	Uni	100	—	16	50	50	—	3-shot fuses		—	—	—	—	—	Lightning
	4,000	4	Wye	Multi	100	—	25	90	10	—	Oil circuit breaker	3	0–15–60 15–30–60	—	—	—	—	Lightning
18	4,160	3	Wye	Uni	100	—	260	65	35	—	Oil circuit breaker	3	10–30–60	—	—	—	—	Wind storm
	4,160	[illegible]	[illegible]	[illegible]	[illegible]	[illegible]	100	[illegible]	[illegible]	[illegible]	Oil circuit breaker	[illegible]	[illegible]	[illegible]	[illegible]	[illegible]	[illegible]	[illegible]

Company Number	Service Voltage	Number of Wires	Connection	Grounding	Per Cent: Open Wire	Per Cent: Cable	Circuit-Miles	Load (Per Cent): Residential	Load (Per Cent): Induction Motor	Load (Per Cent): Synchronous Motor	Equipment: Type	Equipment: Poles	Equipment: Reclosing Cycles (Seconds)	Performance, Per Cent Successful Operations: Initial	Second	Third	Lock-out	Most Common Cause of Outage
19	All	—	—	—	100	—	4,000 10,000	Transformer Distribution			—	—	—	85	10	3	2	Lightning, tree
20	4,150	4	Wye	Multi	100	—	17.5	100	—	—	Oil circuit breaker	3	10-20-60	—	—	—	—	—
	4,800	3	Delta	Isolated	96.3	3.7	111.5	62	38	—	Oil circuit breaker	3	10-20-60	—	—	—	—	—
			Also use 3-pole oil circuit reclosers.															
21	4,160 13,000	4	Wye	Multi	100−	0+	3,000	75	20	5	Oil circuit breaker Older oil circuit breaker	3 3	0-15-60 10-30-36	74	9	2	15	Lightning
	6,600	3	Delta	Isolated	100	—	1,100	—	—	—	Oil circuit breaker	3	?	70	13.5	1.5	15	Lightning
22	2,080 4,160	3	Delta	Isolated	90	10	2,150	-	-	-	Oil circuit breaker	3	15-30-60 7-7-7 20-20-20	80			20	—
23	All	—	—	--	100	-		Induction motor predominates			Oil circuit breaker 2-, 3-shot fuses	3	0-30-60	80	5	1	14	—
24	4,000	4	Wye	Uni	90	10	2,000	20	50	30	Oil circuit breaker	3	0-10-150 0-60-150	87	3	1	9	—
			Three-pole service restorers and oil circuit reclosers used.															
25	12,000	4	Wye	Multi	99	1	800	100−	0+		Oil circuit breaker	3	0-30-60	80	15	4	1	—
26	33,000	3	Delta	Petersen coil	100	—	31	30	40	30	Oil circuit breaker	3	Instant	95	—	—	—	Lightning
27	2,300	3	Delta	Uni	100	—	Short	-			Oil circuit breaker	3	0-20-40	80	20	—	1	—
			Three-pole service restorers and 3-pole oil circuit reclosers used.															
28	12,000	4	Wye	Multi	100	—	5	100			3-shot fuses		—	—	—	—	0	Trees
29	34,500 12,000 2,400	3 4 3	Wye Wye Delta	Uni Multi Uni	100	—	1,500	50	40	10	Oil circuit breaker 2-shot fuses	3	0-15-85	75	20	0+	5	Lightning, trees
30	4,160	4	Wye	Multi	88.8	11.2	647	18.3	50.3	5.6	Oil circuit breaker	3	15-45-90	52.8	2.2	3.4	41.6	Lightning
31	6,900	3	Delta	Isolated	100	—	100	80	20	—	2-, 3-shot fuses		—	—	—	—	1	Lightning, wind
			One-pole service restorers used.															
32	34,500	3	Delta	Uni	100	—	26	95	5	—	Oil circuit breaker	3	5-15-30	—	—	—	—	Lightning
	34,500	3	Delta	Uni	100	—	29	85	15	—	3-shot fuses		—	70	5	5	20	Lightning
	11,000	4	Wye	Multi	100	—	42	90	10	—	Oil circuit recloser	3	3-3-3	80	10	5	5	Lightning, wind
33	4,325	4	Wye	Multi	95	5	2,643	43	57	—	Oil circuit breaker	3	0-30-60	80	7-8	3-4	8-10	Trees
34	4,000 2,300	4 3	Wye Delta	Multi Isolated	100	—	400	70	30	—	Oil circuit breaker	3	15-45-90	90			10	Lightning, trees
35	4,000	4	Wye	Uni	90	10	1,186	65	30	5	45 circuits oil circuit breaker 35 circuits oil circuit breaker	1 3	15-30-90	75	10	4	11	Lightning
			Have some 2-shot fuses.															
36	4,000 5,000	4 3	Wye Delta	Multi Isolated	85 100	15 —	250	65	32	3	Oil circuit breaker—1-, 3-phase 2-, 3-shot fuses		Instant only	75	—	—	25	Lightning
37	4,500	4	Wye	Multi	40	60	550	45	55	-	53-oil circuit breaker 86-oil circuit breaker	1 3	0-30-60	50	23.5	5	21.5	Wind
			Night loading					90	10	—								
38	4,800	3	Delta	Isolated	96	4	12,500	25	45	5	Oil circuit breaker	3	15-60-60	80	5	1	14	Lightning
	4,800	3	Delta	Isolated	100	—	870	100	—	—	Oil circuit breaker	3	15-60-60	84	2	0	14	Lightning
			Has sectionalizing fuses.															
	4,800	3	Delta	Isolated	100	—	985	98	2	—	Oil circuit breaker	3	0-15-30-30	75	18	1	6	Lightning
			Has sectionalizing fuses.															
	4,800	3	Delta	Isolated	100	—	345	100	—	—	Oil circuit breaker	1	5-5-5-5	97	2	—	1	Lightning
			Has sectionalizing fuses.															
	4,800	3	Delta	Isolated	100	—	768	100	—	—	Oil circuit breaker	1	1-1-1-1	93	5	—	2	Lightning
39	4,000	4	Wye	Multi	100	-	125	85	15	—	Oil circuit breaker	3	0-30-60 15-30-75	50	—	—	50	Lightning
	22,000	3	Wye	Uni	100	—	12	95	5	—	3-shot fuses		—	—	—	—	100	—
			Fuses unsatisfactory—to be replaced by oil circuit breaker.															
	22,000	3	Wye	Uni	—	—	22	85	13	2	Oil circuit breaker	3	0-30-60	30	—	—	50	Lightning
	6,600	3	Delta	Isolated	100	—	31	75	25	—	Oil circuit breaker	3	15-30-90	—	—	—	100	—
	6,600	3	Delta	Isolated	100	—	23	75	25	—	3-shot fuses		—	100			0	Lightning
	2,300	3	Delta	Isolated	100	—	20	75	25	—	3-shot fuses		—	100			0	Lightning
	4,000	4	Wye	Multi	100	—	2	95	5	—	Oil circuit breaker	3	15-30-75	98	—	—	—	Lightning
40	2,400 4,160 4,800 7,200	3 4 3 3	Delta Wye Delta Delta	Isolated Multi Iso, uni Iso, uni	100	—	4,800	80	15	5	3-shot fuses Oil circuit breaker	3	0-30-90 4-30-90 30-60-90	54	28	14	4	Lightning
			(30-60-90 reclosing cycle for synchronous motor loads)															
41	2,400 4,160 4,800 8,330 7,200 12,500 11,000 14,400	3 4 3 4 3 4 3 3	Delta Wye Delta Wye Delta Wye Delta Delta	Isolated Multi Isolated Multi Isolated Multi Isolated	100	—	13,800	75	15	10	2-shot fuses 3-shot fuses Oil circuit breaker	3	0-30-60	70	20	5	5	Lightning
			(Uses time delay for initial reclosures for synchronous motor loads as requested by customers.)															

Difficulties which have been encountered in making these applications are:

(*a*). Trouble in obtaining co-ordination with ground relays except in such cases where fuses of low current rating are applicable.

(*b*). Failure to obtain co-ordination between service restorers with instantaneous trip and fuses unless the fuses are of such low current rating that they will blow at currents near the pickup value of the restorer.

Conclusions

As the result of this survey, the following conclusions may be reached:

1. In terms of circuit-miles, immediate initial reclosing is most commonly used for residential loads and 15-second initial reclosing for induction motor loads.

2. Successful initial reclosing was reported for approximately 75 per cent of the trip outs. The second and third reclosures will be successful in approximately 15 per cent of the cases, and lockout can be expected in ten per cent of the cases.

3. Present reclosing equipment appears to meet the requirements of most of the users except that a necessity for changes as noted previously in available oil circuit reclosers or service restorers is indicated.

4. Circuits should be re-energized in 0.5 to 2 seconds if the dropping of induction motor loads is to be avoided.

5. No trouble from restriking of the arc was reported with immediate reclosure with normal speed breakers. Some users, however, feel that an interval of 0.5 second should be provided.

6. The possibilities of using immediate reclosing for motor circuits should receive further study, as there are indications that the advantages of immediate reclosing can be extended to include a much larger percentage of motor circuits than is now being done.

References

1. Engineering report 47 of the Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System. Volume V, July 1941, pages 239–71.

2. Factors Contributing to Improving Electric Service by Means of High-Speed Switching and Utilization of Stored Energy, J. T. Logan, J. H. Miles. AIEE Transactions, volume 60, 1941, December section, pages 1012–16.

3. Experience With Oil Circuit Reclosers on REA Systems, L. M. Moore, B. O. Watkins. AIEE Transactions, volume 62, 1943, August section, pages 531–5.

Appendix. AIEE Committee on Automatic Stations Questionnaire on A-C Automatic Reclosing Equipment

Please give data for each set of circuit and load conditions for which different reclosing practice is used.
Reporting company.......................... Location..........................

A. Circuit and Load Data

Voltage......... Three-wire......... Four-wire......... Delta......... Wye.........
Isolated........ Ungrounded........ Multigrounded........
.... per cent overhead.......... per cent cable
Load.... per cent residential.... per cent induction motor.... per cent synchronous motor

B. Equipment

Repeating fuses two-shot...... three-shot......
Circuit breaker........ pole (single or three) with........ reclosing cycle. Specify most usual reclosing cycle as: instantaneous; 0–30–60 seconds; 15–30–75 seconds; 0–30 seconds, and so forth.
Service restorer.......... pole (single-pole or three-pole)
Oil circuit reclosers............ pole
Protective relay............ (instantaneous overcurrent, time delay overcurrent, and so forth)
Does the equipment automatically change the relaying time after first, second, or third reclosure? .... Give brief description of sequence of operation.

C. Experience Data

Approximate............ years............ circuit-miles
Successful reclosures for reclosing cycle given above
Initial immediate .... per cent. Initial time delay........ per cent
Second........ per cent. Third........ per cent. Lockout........ per cent
What has been found to be the most convenient way of determining the number of first, second, or third reclosures?
Most common cause of outage cleared fast enough for successful immediate initial reclosing. Lightning...... trees...... ....
Reclosing cycle found to be particularly suitable for:

(*a*). Lighting loads..........
(*b*). Synchronous motor loads..........
(*c*). Induction motor load..........
(*d*). Combined motor and lighting loads..........
(*e*). Selective operation with branch circuit fuses..........
(*f*). Selective operation with branch circuit reclosers or service restorers..........

D. General

Do available reclosing relays meet operating requirements, or is some modification in the operating cycle desirable? If so, please give suggested modification.

Do available oil circuit reclosers or service restorers meet operating requirements, or is some modification desirable? If so, give suggested modification. Also give reasons for preferring the single- or three-pole device.

Does the lack of the self-resetting feature in the case of reclosing fuses result in excessive lockouts? What steps do you take (such as patrolling) to keep the number of lockouts to a minimum?

What has been found to be the *maximum* time that the circuit breaker may remain open during a reclosing cycle, without dropping any appreciable part of the motor load? State whether load is predominately induction or synchronous.

Give operating data which indicate *minimum* time that circuit breaker may be reclosed with assurance that fault arc path has been given sufficient time to deionize. In answering this question, consideration should be given to the effect of counter electromotive force from synchronous machines (connected to the feeder) which tend to sustain the fault arc while the circuit breaker is open.

What has been your experience in obtaining successful co-ordination between reclosing equipment and fuses, the latter being located in some subdivision of the feeder?

What has been your experience in obtaining successful co-ordination between oil circuit reclosers or service restorers, and fuses, the latter being located in some subdivision of the feeder?

Any additional comments:

This questionnaire is limited to stub feeders which are energized from one source only and which do not require voltage checking devices, synchronizing relays, and so forth.

A Capacitance Bridge for Determining the Ratio and Phase Angle of Potential Transformers

H. W. BOUSMAN
MEMBER AIEE

R. L. TEN BROECK
ASSOCIATE AIEE

Synopsis: A capacitance bridge for the calibration of instrument potential transformers up to 300 kv is presented in this paper. This bridge provides measurements which are correct within 0.1 per cent on ratio and 3 minutes on phase angle. There are within the bridge arrangements for testing its accuracy as determined by the stability of its component arms. It is particularly desirable to have this means of self-testing if transformers are to be tested in a laboratory which does not have extensive standardizing facilities. Further advantages beyond accuracy for standardization purposes are flexibility and ease of operation for production testing, and low cost and small volume as compared to a high-voltage resistance potentiometer.

IN the determination of ratio and phase angle of potential transformers, measurements are required which are correct within 0.1 per cent on ratio and three minutes on phase angle. Such measurements are made on transformers with voltage ratings as low as 110 volts and as high as 161 kv. It is probable that higher voltages may be used in the future.

A capacitance bridge has been developed for this purpose. This bridge is connected first as a Schering Bridge in order to establish the relative impedance of its arms; then it is connected one part to the primary winding of an instrument potential transformer and the other part to the secondary winding of the transformer in order to determine the ratio and phase angle between the windings of the transformer.

This method of measuring the properties of transformers may be compared to the resistance potentiometer method[1,2] and to previous proposals for capacitance bridge methods.[2,3,4,5] The most conspicuous advantage of this method over the resistance-potentiometer method is the reduction in weight and space required. The advantage over previous proposals of a capacitance bridge is provision for reconnecting the bridge in order to establish its accuracy in place.

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H. W. BOUSMAN is in the general engineering laboratory, General Electric Company, Schenectady, N. Y. R. L. TEN BROECK is electrical engineering assistant, General Electric Company, Pittsfield, Mass.

Performance

A bridge of this design was installed in the transformer section of the Pittsfield works' laboratory and used in conjunction with the 132-kv shielded resistance potentiometer[1] already there. It was operated for a period covering both the winter and summer seasons.

The relative size of the capacitance bridge made it very attractive, as it required 200 square feet of floor space whereas the resistance potentiometer required 900 square feet. The saving of factory space allows a saving in testing because it facilitates the handling of transformers during this stage of manufacture.

Transformers have been tested throughout the range 110 volts to 161 kv. The extreme flexibility of the capacitance bridge was an appealing point to the operator. A 1400:1 ratio could be checked as easily as a 20:1. The change from one voltage or ratio to another could be done without changing high-voltage taps on the equipment. Odd ratios were just as simple to measure as were the integral ratios.

A 50 micromicrofarad high-voltage compressed-gas capacitor was used for ratios down to 10:1. Below 10:1 ratio a 200-micromicrofarad shielded air capacitor was used. This combination of standard capacitors provided good sensitivity over the whole voltage range.

After this experience a permanent installation was decided upon. A new bridge was built and installed and has been in use for over a year. This bridge provides measurements which are correct within 0.1 per cent on ratio and three minutes on phase angle throughout the voltage range used thus far. The bridge voltage rating is 300 kv, and no difficulty is expected in extending measurements to this voltage.

The Bridge Circuit

This bridge has two resistance arms and two capacitance arms as shown in Figure 1*a*. Two of these arms, R_3 and C_2, are fixed in magnitude. C_2 is a shielded gas capacitor for high-voltage use. The other arms, R_4 and C_1, are continuously adjustable. If R_4 is adjusted to a conductance $\sqrt{n}$ times that of R_3, where n is the ratio of the transformer to be tested, the bridge can be balanced by adjusting C_1 to a capacitance $\sqrt{n}$ times that of C_2. Now suppose C_1 and C_2 interchanged and the transformer connected so that the high-voltage winding is connected to C_2 in series with R_4 and the low-voltage winding to C_1 in series with R_3, Figure 1*b*. The smaller capacitance, C_2, is now in series with the larger conductance, R_4, but the voltage applied to this combination is n times that applied to the larger capacitance in series with the smaller conductance. Thus the voltage drop across R_3 is again equal to that across R_4 and the bridge again in balance excepting a small phase shift explained in Appendix I. If the transformer ratio differs slightly from n, the small change in R_3 or R_4 required to restore balance is a measure of this departure. Used in this manner, the bridge gives evidence of its own stability before each measurement on a transformer. This self-standardizing feature is particularly desirable if transformers are to be tested where extensive standardizing facilities are not available.

A detailed explanation of this bridge circuit for measuring the ratio and phase angle of a transformer is developed in Appendix I. The bridge connections finally arrived at are shown in Figure 4. In this figure R_{33} is a bridge resistance arm adjustable from 9,900 ohms to 10,110 ohms, set at 10,000 ohms when the ratio correction factor is unity. The adjusting dials are marked directly in ratio correction factor. Adjustment for phase angle is made by changing C_{33}, and C_{33} is marked in such units that it reads directly in phase angle at 60 cycles when R_{33} is at 10,000 ohms. Departure from direct reading in phase angle at other values of R_{33} may be corrected by reference to a curve in any case and is minimized by care in the design of the $R_{33}C_{33}$ arm of the bridge so that no correction is required on the greater portion of transformer tests.

R_{34} in the bridge is made of a conductance decade; that is, of resistors connected in parallel by a switching sequence, marked in terms of $\sqrt{n}$ where n is the nominal transformer ratio. The

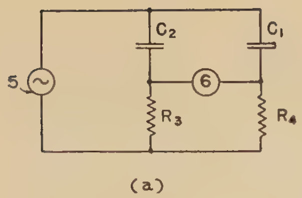
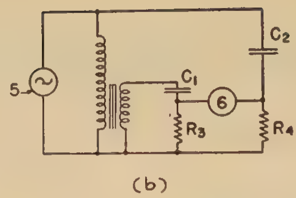


Figure 1. Basic connection diagrams

(a). Schering bridge
(b). Transformer bridge



conductances of the principal resistors in R_{34} are 1, 2, 2, 5, 10, 20, 20, and 50 times the conductance of the 10,000-ohm setting of R_{33}.

Note particularly that errors in the ratio of R_{33} to R_{34} are doubled in their effect on the determination of potential transformer ratio. This is not a serious disadvantage because the conductance decade arrangement, in which most of the conductance of R_{34} is made up of steps of $^1/_2$, 1, and 2 times 10^m where m is an integer, lends itself especially to precise and accurate determination of the ratio between R_{33} and R_{34}.

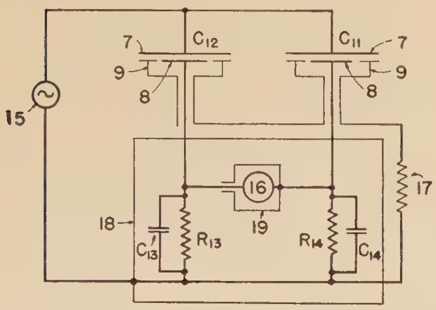


Figure 2. Connection diagram of Schering bridge including shielding

Capacitor C_{32} in Figure 4 is of fixed capacitance and suitable for use at high voltage. C_{31} is an adjustable low-voltage capacitor with worm drive for precise setting while balancing the bridge. Any difference in dielectric loss angle between these capacitors appears double as an error in transformer phase-angle measurement. This error will not exceed one-quarter minute of angle so long as the capacitor at C_{31} is kept fairly dry.[7]

The Bridge

The high-voltage capacitor which constitutes one bridge arm, C_{32} of Figure 4, has shielded concentric cylindrical electrodes operated in carbon dioxide at about 100 pounds per square inch gauge pressure. Figures 5 and 6 show the construction. The essential properties and advantages of standard capacitors constructed in this manner have been set forth elsewhere[6,7] and will merely be summarized here.

(*a*). Change of dimensions with voltage is calculably negligible.

(*b*). Change of dimensions with temperature and its effect on capacitance is calculable.

(*c*). Change of capacitance with alignment is calculable; thus, limits within which it is negligible may be established.

(*d*). The maximum dielectric loss angle encountered in such capacitors is small as compared to usual phase-angle accuracy requirements, that is, much smaller than one minute at 60 cycles.

With respect to factor *b*, the massive tank by its thermal slowness limits temperature gradients inside the capacitor. A steady-state temperature change of five degrees centigrade or a change in the temperature difference between electrodes of two degrees centigrade is required to produce 0.01 per cent effect on measured ratio. In order to produce this effect, these changes must occur between the standardization in terms of bridge resistors and the measurement on the transformer, a matter of a few minutes at most.

This capacitor may be operated at voltages up to 300 kv rms.

The other parts of the bridge are housed in the case shown in Figure 7 and Figure 8. Note at the top of Figure 8 the parallel plate capacitors used at C_{31} of Figure 4. One of these has a worm drive which allows adjustment to 0.005 micromicrofarad. The other two are of fixed capacitance and may be switched into circuit to extend the capacitance range. All have plates supported from a guard structure with the solid insulation shielded from the field between plates.

Dielectric loss angle of these capacitors is ordinarily less than 0.00002 or four seconds of angle. The accuracy of phase-angle measurement depends upon the maintenance of a small loss angle. Proof of such maintenance is obtained from the standardization balance of the bridge before each measurement. Any change in C_{34} of Figure 4 from the customary value is evidence that the relative dielectric loss angle of the air capacitors has changed. In practice, only the low-voltage capacitor changes, and it changes only because of humidity and dirt.[7] Ordinary care in maintenance will avoid such changes.

The resistance arms of the bridge are partly visible in Figure 8, behind the galvanometer amplifier case. The principal resistors are of manganin. The one micromho steps are of advance alloy, and the smaller conductance decades are carbon resistors. These latter serve only for fine adjustment of the $\sqrt{n}$ arm, hence errors of several per cent are allowable.

The galvanometer and its amplifier are in a case within the main bridge case.

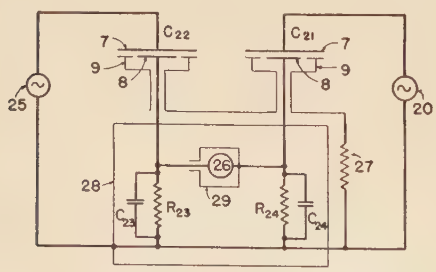


Figure 3. Connection diagram, capacitance bridge for comparing two voltages

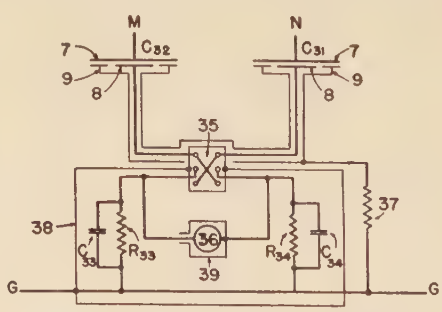


Figure 4. Connection diagram of bridge for measuring the ratio and phase angle of potential transformers

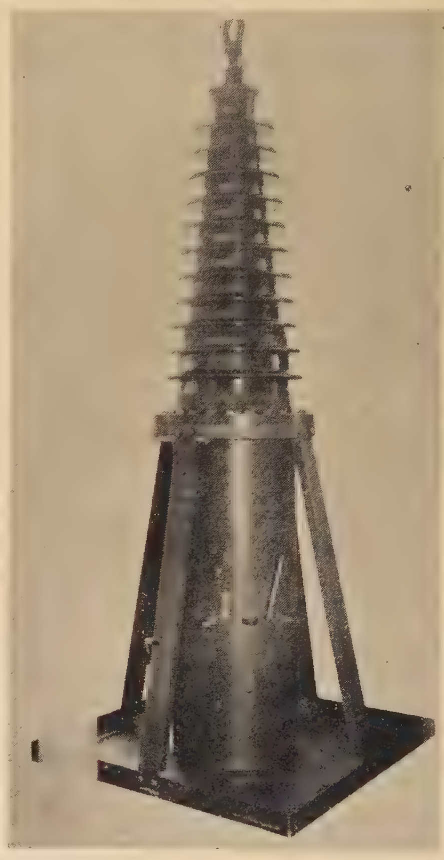


Figure 5. Compressed-gas capacitor, 300 kv

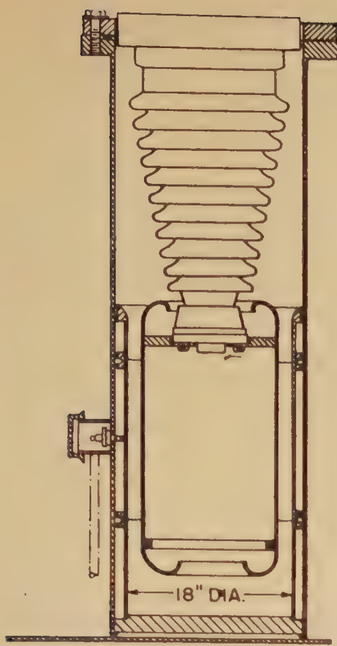


Figure 6. Cross section of the 300-kv capacitor

The capacitance between cases is a part of C_{34}.[3] It is not affected by switching connections and therefore not a factor in the accuracy of the bridge.

The lower part of the bridge case has relays which connect an auxiliary power supply for the calibrating balance of the bridge and which may be used for remote control of high-voltage switching.

Figure 7. Bridge for measuring ratio and phase angle of potential transformers

Measurement Procedure

The operation of the bridge is as follows:

1. Set ratio-correction-factor dials at 1.0000; set phase-angle dials at a value determined by the ratio of the transformer to be tested; set $\sqrt{n}$ dials at the square root of the nominal transformer ratio; then turn selector switch to "calibrate."

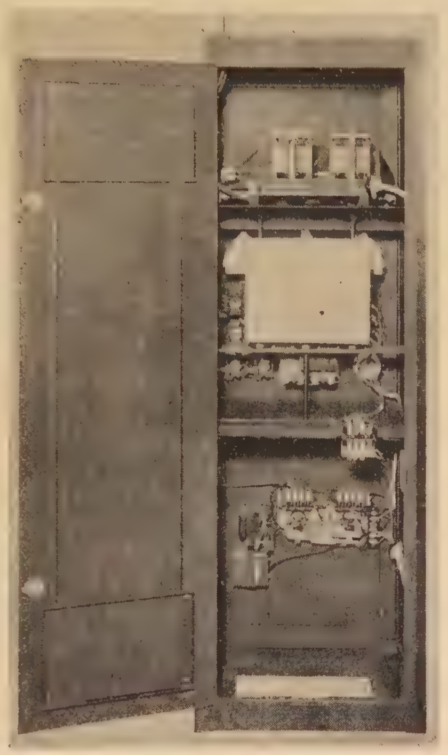


Figure 8. Back view of the bridge, case open

2. Apply power to calibrating circuit, and balance the bridge by adjusting the low-voltage air capacitor C_{31} and the capacitance C_{34} of Figure 4.

3. Connect transformer to be tested and turn selector switch to "measure."

4. Apply power to transformer and balance the bridge by adjusting the ratio-correction-factor and phase-angle dials at each desired burden and excitation. Ratio correction factor of the transformer is read directly from the dials. Phase angle is read directly from the dials on tests at 60 cycles. At other frequencies the dial reading must be multiplied by the ratio of the test frequency to 60.

Standardization of the Bridge Resistance Arms

Accuracy of the transformer-ratio measurement rests on the accuracy with which resistance ratio between the bridge arms is known. The ratios of the various R_{34} settings to R_{33} are preferably determined in place using a step-by-step procedure which imposes conditions equivalent to those of actual use. A schedule has been established whereby these ratios can be determined after about one hour's systematic work. This procedure has been confirmed by measuring the resistances separately in terms of resistance standards which in turn have been compared to standards maintained at the National Bureau of Standards.

Changes in the present bridge since the initial standardization have been confined to the expected trivial slow drift in relative resistance of the arms, but it must be recognized that a significant change is possible at any time, although it is not expected. Therefore, the operator should be alert for any irregularity in the "calibrate" balance before each measurement and, in addition, should carry out occasionally the schedule established for determining resistance ratio between the arms. That this can be done without external standards is an advantage.

Summary

A bridge has been developed for measuring the ratio and phase angle of instrument potential transformers. The measurements are correct within 0.1 per cent in ratio and three minutes in phase angle. The bridge rating is 300 kv, but actual use thus far has been limited to tests at 161 kv and below. This limit was imposed by the transformers requiring test, and extension of tests to higher voltage, when transformer design requires it, offers no difficulty.

Appendix I

Consider Figure 1a in which C_1 and C_2 indicate capacitance while R_3 and R_4 indicate electrical resistance. If an alternating current is supplied by generator 5, the condition for no resultant potential across a circuit element 6, such as a galvanometer, is that:

$$C_1/C_2 = R_3/R_4 \tag{1}$$

When it is sought to realize the circuit of Figure 1a in practice, it becomes necessary to take into account any departure of actual capacitors from being pure capacitance and any residual inductance or capacitance of the resistors which may be used where resistance is indicated in the figure. In addition, capacitance from conducting parts of the apparatus to earth and to other apparatus not shown may affect the distribution of current from the generator. Figure 2 shows diagrammatically the arrangement of apparatus which results when these factors are considered.

In Figure 2, C_{11}, and C_{12} are capacitors each having electrodes 7 and 8 and further electrodes 9 which entirely surround electrodes 8, excepting the exposure of electrodes 8 to electrodes 7. Electrodes 7 and 8 are supported from electrodes 9 by separate pieces of solid insulating material so that no solid insulation lies in the region between electrodes 7 and 8. This region is filled with air or other gas.

R_{13} indicates the resistance of a resistor. The residual inductance or capacitance of this resistor, expressed as capacitance, together with any deliberately introduced capacitance, is shown as C_{13}. Similarly, R_{14} and C_{14} indicate another resistor with its associated capacitance, whether deliberately introduced or accidental. A galvanometer or other voltage-detecting means is shown at 16. A conducting case 18 may enclose circuit elements 13, 14, and 16 to shield them from external electric field. If the galvanometer 16 is shielded by a shield 19 connected as shown in the figure, the capacitance from shield 19 to case 18 must be included as part of the capacitance C_{14}. There are also other means of successfully shielding galvanometer 16.

Suppose alternating current is supplied by generator 15, and suppose further that the circuit elements are adjusted until no voltage exists at galvanometer 16. Suppose that impedance 17 has been adjusted until no voltage exists between conductors 8 and 9 of the capacitors. Under these circumstances the current through the impedances indicated by the subscripts 13 and 14 will be exclusively the charging current through the direct capacitance between plates 7 and 8 of capacitors C_{12} and C_{11}, respectively. The necessary and sufficient relationships between the impedances indicated by the subscripts 11, 12, 13, and 14 are:

$$C_{11}/C_{12}=R_{13}/R_{14} \tag{2}$$

and

$$R_{14}(C_3+C_{11})-R_{13}(C_{13}+C_{12})=0 \tag{3}$$

combining equations 2 and 3 gives the simple relation between the impedances indicated by subscripts 13 and 14:

$$R_{14}C_{14}-R_{13}C_{13}=0 \tag{4}$$

If, now, an imperfect dielectric is introduced between the plates 7 and 8 of one capacitor, say capacitor 12, the angle A by which the vector representing the charging current of this capacitor lags that representing the charging current of a pure capacitance is given by the expression, valid for angles so small that the angle expressed in radians approximately equals the tangent of the angle:

$$A=\omega(R_{14}C_{14}-R_{13}C_{13}) \tag{5}$$

where $\omega=2\pi$ times the frequency in cycles per second. This is the Schering bridge (Thomas bridge) commonly used for dielectric measurements.[10,11]

Consider now Figure 3, similar to Figure 2 except that generator 25 supplies current to capacitor C_{22} while separate generator 20 supplies current to capacitor C_{21}. Suppose that these generators have voltages in phase with one another, but that the voltage of generator 25 is n times the voltage of generator 20. The condition for no voltage at galvanometer 26 is:

$$\frac{\dfrac{nR_{23}}{1+j\omega R_{23}C_{23}}}{\dfrac{R_{23}}{1+j\omega R_{23}C_{23}}+\dfrac{1}{j\omega C_{22}}}=\frac{\dfrac{R_{24}}{1+j\omega R_{24}C_{24}}}{\dfrac{R_{24}}{1+j\omega R_2\,C_{24}}+\dfrac{1}{j\omega C_{21}}} \tag{6}$$

The expanded statement of the relation between the impedances indicated by the subscripts 21, 22, 23, and 24 necessary to satisfy this condition is unwieldy in length. If the impedances are selected so that

The impedance of C_{21} is much greater than R_{24}

The impedance of C_{22} is much greater than R_{23}

$\omega R_{23}C_{23}$ is much less than 1

$\omega R_{24}C_{24}$ is much less than 1

certain approximate relations are sufficient. These follow:

$$C_{21}/C_{22}=nR_{23}/R_{24} \tag{7}$$

$$R_{24}(C_{24}+C_{21})-R_{23}(C_{23}+C_{22})=0 \tag{8}$$

If there is a difference B in phase angle between the voltages from generators 25 and 20, small enough that the angle expressed in radians may be assumed equal to the tangent of the angle,

$$B=R_{24}(C_{24}+C_{21}')-R_{23}(C_{23}+C_{22}) \tag{9}$$

It is evident that the bridge of Figure 3 may be used for the measurement of the ratio and phase angle between voltages such as the primary and secondary voltages of an instrument potential transformer, since equations 7 and 9 enable the calculation of ratio and phase relationship from the observed values of C_{21}, C_{22}, R_{23}, C_{23}, R_{24}, and C_{24}.

If such comparison is to be made with great accuracy, it is necessary to consider the accuracy with which the value of the several circuit elements may be known. Consider equation 7. Resistors are commonly prepared of alloy wire little enough affected by temperature variations and of such permanence that the resistance may be known within 0.01 per cent. Air capacitors are not made readily with such accuracy. Consider now equation 9. This equation is based on equality of phase angle between current and voltage in the two air capacitors represented by C_{21} and C_{22}. This condition may be realized within some few seconds of angle. However, uncertainties in the value of C_{23} and C_{24} may lead to errors in usual practice nearly as large as one minute of angle in the determination of the phase angle B.

It would be desirable to determine the ratio and phase angle between two voltages in terms of the more accurately definable quantities, that is, the resistances R_{23} and R_{24} and the purity of the capacitances at C_{21} and C_{22}. For a method of accomplishing this end, refer to Figure 4. In this figure, C_{32} is a fixed air or gas capacitor, shielded and guarded in the fashion previously described. C_{31} is an adjustable air capacitor so shielded and guarded. It is required of C_{31} that once it is adjusted, it shall, if not touched, remain constant in direct capacitance between its electrodes 7 and 8 until the measurements described hereafter are completed. It is not required that any particular adjustment shall be reproducible at a later time. Resistances R_{33} and R_{34} shall be adjustable so that

$$R_{33}/R_{34}=r \quad (10) \qquad \text{where} \qquad r^2=n \quad (11)$$

and n is the nominal or approximate ratio of two voltages, nearly in phase, which are to be compared with respect to ratio and phase angle. In making this adjustment it is convenient but not necessary to have the resistance in ohms of R_{33} a round number while R_{34} is adjustable to secure the ratio r. The capacitance associated with R_{33} is, for convenience in showing the working of this test method, assumed to be made up of two parts, an adjustable calibrated portion C_{33} and a small unknown capacitance X, which will not change provided R_{33} is unchanged during the course of the measurements. Similarly, with R_{34} is associated the known adjustable capacitance C_{34} and the unknown fixed capacitance Y.

In the use of this method, a source of alternating voltage is connected from M and N to G, Figure 4, and the switch 35 is positioned to connect C_{31} to R_{34} and C_{32} to R_{33}. C_{33} is adjusted to some arbitrary small capacitance; R_{34} is adjusted in accordance with equation 10; then C_{34} and C_{31} are adjusted until galvanometer 36 indicates equality of the voltages across R_{33} and R_{34}. By analogy to equation 2, this is true when

$$C_{31}/C_{32}=R_{33}/R_{34} \tag{12}$$

whence, from equation 10,

$$C_{31}/C_{32}=r \tag{13}$$

From analogy to equation 4

$$R_{34}(C_{34}+Y)-R_{33}(C_{33}+X)=0 \tag{14}$$

whence

$$C_{34}+Y=(R_{33}/R_{34})(C_{33}+X) \tag{15}$$

using equation 10

$$C_{34}+Y=r(C_{33}+X) \tag{16}$$

Now the switch 35 is changed so that C_{31} is connected to R_{33} and C_{32} to R_{34} while separate voltage sources are connected from M to G and from N to G. The voltage at M is n times that at N and in phase with that at N. Now by analogy with equation 7, equality of voltage across R_{33} and R_{34} will be secured when

$$C_{31}/C_{32}=nR_{34}/R_{33} \tag{17}$$

substituting from 10 and 13, $r=(n/r)$ therefore, $n=r^2$ which is simply equation 11. Thus, R_{33}, R_{34}, C_{32}, and C_{31} need not be changed if the ratio between voltages at N and M is actually n. However, C_{33} or C_{34} will have to be changed, as may be seen from analogy to equation 8. Suppose C_{33} is unchanged, but C_{34} is changed to a new value C_{34}' in order to secure equality of voltages across R_{33} and R_{34}:

$$R_{34}(C_{34}'+Y+C_{32})-R_{33}(C_{33}+X+C_{31})=0 \tag{18}$$

$$C_{34}'+Y=r(C_{33}+X+C_{31})-C_{32} \tag{19}$$

Subtract equation 16

$$C_{34}+Y=r(C_{33}+X) \tag{16}$$

$$C_{34}'-C_{34}=rC_{31}-C_{32} \tag{20}$$

but, from equation 13

$$rC_{31} = r^2C_{32} = nC_{32} \tag{13}$$

therefore,

$$C_{34}' - C_{34} = (n-1)C_{32} \tag{21}$$

Note that X and Y need not be known. Any departure of the ratio between the two voltages from the assumed value will require a proportionate change in either R_{33} or R_{34}. Similarly, if the two voltages do not agree in phase, either C_{33} or C_{34} will need to be changed. Specifically, if C_{34} is changed to C_{34}' as indicated in equation 21, any further change necessary will be due to a phase difference D, expressed in radians.

$D = \omega R_{33}$ times the change in C_{33}

or

$D = \omega R_{34}$ times the change in C_{34}, whichever is changed.

In the operation of the bridge for instrument potential-transformer calibration, it has been found most convenient to mark C_{33} in minutes of angle at 60 cycles. Then the calibrating balance of the bridge (as a Schering bridge) is made with C_{33} set at such a value (in minutes at 60 cycles) as that required for the change from C_{33} to C_{33}' when the arms are reversed.

$$C_{33} - C_{33}' = [(n-1)C_{32}]/r \tag{22}$$

This makes C_{33}' the capacitance marked "0 minutes" on the dial.

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Carrier-Current Differential Protection for Transformer Banks

T. A. CRAMER
ASSOCIATE AIEE

When carrier current was first applied to protective relaying of the directional type, it consisted of a simple telegraph channel. Subsequent developments have expanded the capabilities to the extent that each carrier channel for pilot relaying now may provide one or more of the following additional services:

1. Simplex telephony for testing or emergency use.
2. Telemetering or load control.
3. Supervisory control of unattended substations.
4. Remote control of circuit breakers or sectionalizing switches.
5. Transferred tripping to provide differential protection for transformer banks used with only a low-voltage circuit breaker.

Situations frequently arise where a power-system planning or designing engineer finds it necessary to install a power transformer with only a low-voltage breaker. This is particularly true under present conditions where new plants must be served with as little additional equipment as possible. In many cases, this resolves into extending a line from an existing station or bus and terminating it with a transformer bank. Another common arrangement is to tap the transformer bank on an existing line, using only a low-voltage breaker.

Both of these arrangements present a problem for the relay engineer, especially the protection of the transformer bank. The standard line relays give some protection for transformer faults but usually cannot be set for the required sensitivity. Some conventional type of differential relay can be used to detect faults in the transformer and cause tripping of the local low-voltage breaker, but this does not clear the fault. If the fault is on the low-voltage winding, or close to the ground end of the high-voltage winding, there is a good possibility that the remote relays will not have the required sensitivity to trip the remote circuit breaker.

In this case it becomes necessary to convey the tripping indication from the local differential relay to the remote circuit breaker, and this can be accomplished by pilot-wire circuits, carrier-current channels, and so forth. Another method of tripping the remote breaker is to place a fault on the high-voltage side by means of a shorting switch which will produce a fault of sufficient magnitude to operate the remote trip relays. It is the purpose of this paper to describe a simple and reliable method of remote tripping transmitted over a carrier-current pilot relaying channel.

The accepted practice for carrier-current pilot relaying channels is to operate them as simple telegraph channels which transmit carrier to hold a blocking relay in the nontrip position at the ends of the line section. In this type of operation, the channel is in use only a short portion of the time for relaying or test, and it is customary to include modulation equipment to provide a testing or emergency telephone channel. The addition of the remote tripping function to the carrier-current channel improves the use factor and requires less critical material than other methods.

As previously stated, the pilot relaying equipment for protection of the transmission line operates on a blocking principle. The carrier transmitter is maintained in a stand-by condition at all times, with full voltage on plate and cathode-heater circuits, but the screen grids of the transmitter tubes are maintained at a negative potential with respect to the cathodes, which prevents the transmitter from operating. When the contacts of the fault-detector relays operate, the screen grids immediately change to a positive potential, and the carrier transmitter is started. If the fault is within the range of protection of the relays, the directional relays operate to re-establish

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T. A. Cramer is in the radio transmitter engineering division, electronics department, General Electric Company, Schenectady, N. Y.

the grid bias on the transmitter tubes and stop carrier transmission. As soon as the carrier blocking signal is removed, the receiver relay is free to complete the circuit through the contacts of the tripping fault-detector relay to the trip coil. After the blocking signal is removed, the receiver relay drops out in about five milliseconds. The completion of the trip circuit operates a seal-in relay which bypasses all other relay contacts and holds the trip circuit energized until the circuit breaker opens.

It can be seen that with this mode of operation, momentary blocking of the receiver relay by interference produced by extremely short-time transients, such as lightning, will have no appreciable effect on the successful operation of the relaying system.

Operating time is an important consideration in any protective relaying scheme, and this speed must be accomplished with satisfactory accuracy. Where this tripping signal is being superimposed on an existing channel, also having high speed requirements, it becomes necessary to make some compromise in order to obtain simplicity of equipment. Insofar as the transformer bank is concerned, the tripping time should be instantaneous to limit the extent of the damage. In many cases where no provision is made for tripping the remote breaker, the transformer fault will remain on the system until a telephone call can be put through to the remote station, or events will follow their usual course with the fault spreading until the current is sufficient to operate the remote line-protection relays.

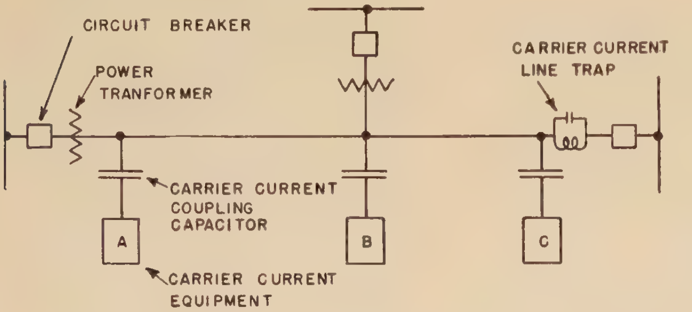

Figure 1. One-line schematic diagram of typical remote-trip installation

A. Transmits and receives remote-trip signal
B. Transmits and receives remote-trip signal
C. Receives remote-trip signal

When the blocking system is inverted and a *tripping* signal is sent instead of a blocking signal, it will be noted that an operation of the receiver relay by interference cannot be tolerated. Since there are no remote fault detectors which recognize the fault in the transformer bank, it is necessary to entrust to the carrier-current channel the complete job of tripping the remote breaker.

There are many possible methods of conveying this remote tripping information over the carrier-current channel. Some of these may be summarized as follows:

1. Keyed code. This requires no change in carrier set but requires mechanical coder and decoder or selector mechanism.

2. Modulating tone applied to carrier. This requires an oscillator at the transmitting end and a filter unit at the receiving end.

3. Frequency shift of carrier. This requires a means for shifting the carrier frequency at the transmitter end and a special receiver to recognize this shift in frequency either by a heterodyne method, by a balanced discriminator, or by simply shifting carrier far enough to avoid interference from the normal carrier frequency.

4. Some combination of the foregoing methods, such as keyed modulation, modulation applied as frequency modulation, or combinations of more than one modulating tone.

Between the two extremes of instantaneous tripping and not tripping at all, a maximum relay time of 0.5 second was selected as a reasonable value which would make it possible to obtain the required accuracy with simple equipment.

The keyed-carrier method was first investigated since it had the outstanding advantage of requiring no changes in the carrier equipment. There are various factors, however, which offset this advantage since the complications are transferred to the selector mechanism, and these complications increase as the tripping time is decreased. Where this remote tripping function is to be added to an existing carrier channel of older design equipment which did not provide modulation facilities, or a receiver with a satisfactory audio output, there is considerable merit in an arrangement which employs keyed carrier and a selector, although the relay time is increased.

One such arrangement may consist of a transmitter which sends short impulses at a uniform rate of ten per second, and a selector mechanism which will reject any other combination of dots, dashes, or spaces in transmission. Ten consecutive impulses must be received in order to trip the breaker, and the code is either repeated several times or the transmitter runs continuously until the hand-reset auxiliary tripping relay associated with the transformer differential protection is reset. The ten-impulse code thus is repeated several times to assure tripping, since any interference received just before or during a tripping signal may mix up the code and prevent operation. The continued-impulse type of code provides a short recovery time after such an interference.

The carrier-frequency shift or frequency-modulation method requires a separate receiver and introduces some complexity in the transmitter. While this arrangement has been applied in certain cases, the audio-tone method is easily added to existing equipment and has some very practical advantages.

After due consideration of the various points of each method, the arrangement using an audio modulating tone was selected to convey the tripping information over the carrier channel. Factors influencing this decision were:

1. The necessary speed of operation could be obtained with the required selectivity.

2. The equipment could easily be added to the standard equipment for pilot relaying.

The design of this modern carrier equipment provided the necessary modulator and a receiver with satisfactory audio-output characteristics, while the oscillator and filter units were in production as components of other standard carrier-current equipment.

The details of the over-all operation of this system may be summarized as follows: At the sending end, the occurrence of a fault in the transformer bank operates the differential relay, which in turn operates a hand-reset multicontact auxiliary relay. One set of contacts on this relay starts carrier transmission, and another contact starts the audio-oscillator unit which usually is adjusted for a frequency of 3,000 cycles. This 3,000-cycle tone modulates the transmitter output. At the receiving end of the carrier channel, the receiver demodulates the incoming signal, and the resulting 3,000-cycle audio tone is fed into the input of the filter unit. The filter unit consists of a vacuum tube operated as a relay tube with sufficient negative bias to prevent plate current from flowing under stand-by conditions. The tuned circuit is connected across the input to the control grid and adjusted to 3,000 cycles. This arrangement attenuates all frequencies except the 3,000-cycle signal, which, when applied to the grid of the relay tube reduces the bias voltage, and plate current flows in the relay circuit to operate the audio relay. The audio relay

is provided with an adjustable time delay of 0.1 to 0.5 second to further improve the over-all selectivity of the equipment by preventing operation on nearby lightning strokes or other causes of interference. When this relay operates, it energizes a hand-reset auxiliary which trips the circuit breaker.

Details of Equipment

The basic carrier equipment for pilot relaying consists of a transmitter-receiver unit, a coupling capacitor to couple the signal to the high-voltage line, and a line trap to terminate the protected line section in a high impedance to prevent the short-circuiting of the carrier channel by external line-to-ground faults on the coupling phase. The equipment is built in units of a standardized width of 19 inches, and a panel space height that is a multiple of $1^3/_4$ inches. The following paragraphs give a general description of each of the various units which are used to make up a given assembly.

Transmitter-Receiver Unit

This standard equipment is designed for operation directly from either a 125-volt or 250-volt station storage battery, with a carrier frequency range of 50 to 150 kilocycles. The transmitter employs five type-25*B6G* tubes in a master-oscillator power-amplifier circuit. The receiver employs two tubes, one 25*A7G* as a detector and audio tube, and one 25*B6G* as a relay tube.

An important feature of the receiver is the separation of the telephone function from the pilot relaying function. The incoming carrier signal is rectified by a diode unit in the 25*A7G* tube. The resulting voltage is filtered slightly and fed to the grid of the relay tube. This circuit is designed to have a limiting characteristic so that the relay current is essentially constant for all signals within the working range of the receiver. Since changes in incoming signal voltage produce practically no change in plate current, it is obvious that this tube cannot be used to obtain audio output from the receiver. For this reason the multisection type-25*A7G* tube is used since it contains another section which can be used as an audio amplifier. A portion of the a-c voltage developed across the diode-rectifier load resistor is coupled to the input circuit of this amplifier. This arrangement provides the desired characteristics for the relaying and the telephone function and permits obtaining sufficient audio output to operate the audio relay units used for the remote-tripping function.

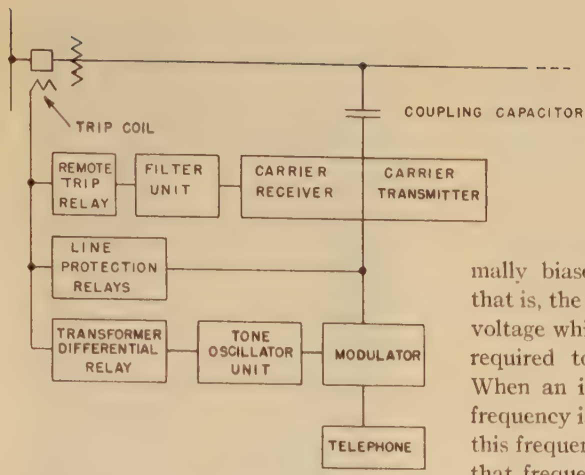

Figure 2. Block diagram of carrier-current equipment showing arrangement for pilot relaying, sending and receiving remote-tripping signal, and telephone

Modulator Unit

The modulator unit employs two type-25*B6G* tubes in a push–pull amplifier circuit, the output of which is applied to the screen grid circuits of the transmitter to modulate the carrier-current output. The 3,000-cycle tripping signal from the oscillator is fed directly into the modulator unit ahead of the speech input volume control in order to obtain independent control of both of these functions. A 3,000-cycle filter is added to the microphone circuit to eliminate this frequency from the voice circuit. This filter has negligible effect upon the intelligibility of the voice signal since the bulk of the voice frequencies lie below 2,000 cycles.

Audio-Oscillator Unit

The oscillator unit required for the remote tripping signal usually is adjusted to 3,000 cycles. This adjustment is made by changing the capacitor combinations and moving the iron core in the inductance. The oscillator tube is a twin triode, one side of which is used for the oscillator, while the other side serves as a buffer amplifier to prevent changes in frequency due to changes in external loading. A filter is provided on the power supply to eliminate the possibility of any ripple on the station battery voltage supply from being amplified and fed into the modulator unit. The oscillator is normally in a stand-by condition and is started by completing the cathode circuit to the negative power supply. The output voltage of the oscillator is adjustable to control the degree of modulation applied to the carrier wave.

Filter Unit

The filter unit is designed to work with the audio-oscillator unit, and contains two tuned circuits which are adjustable over the same range as the oscillator unit. Each tuned circuit is connected across the input circuit of a relay tube which is normally biased so that no current flows; that is, the control grid is maintained at a voltage which is more negative than that required to cut off the plate current. When an incoming signal of the proper frequency is received, the circuit tuned to this frequency offers a high impedance to that frequency and permits a voltage to appear on the control grid circuit of the associated relay tube. This voltage is alternating, of course, and on the positive swings the control grid voltage is made positive thus causing plate current to flow in the relay circuit.

Sockets are provided on the filter unit for mounting two relays. In this case, the relay associated with the 3,000-cycle tone is located on the switchboard while the relay for other tone is mounted on this unit.

Relay Unit and Accessories

The adjustable time delay on the tripping relay is obtained by means of a small auxiliary relay with a copper slug on the coil, and the time is adjustable by varying the air gap and spring tension. This relay is picked up normally, and the time is obtained on the drop-out. The coil of this time-delay relay is energized from the station battery, through normally closed contacts on the audio receiver relay which is mounted in the same enclosing case. When an audio signal is received to produce tripping, the audio receiver relay picks up, thus interrupting the coil circuit of the time-delay drop-out relay, and at the same time it closes one set of contacts in the circuit to the type-*HEA* hand-reset auxiliary relay. When the time-delay relay closes its contacts, it completes the circuit to the *HEA* and trips the circuit breaker.

Every protective device requires provision for test and maintenance, and this is provided for by a test switch, a milliammeter to read the audio-receiver-relay current, and a signal lamp to show when a tripping signal is being received. The purpose of this signal lamp is to warn the operator that the *HEA* relay should not be reset since the tripping signal is still on the line.

Some Details of Operation

Whenever two or more services are placed on any transmission medium, it be-

comes necessary to consider the possibility of interference between these services. The addition of telephony on a pilot-relaying channel is a good example where it is a simple matter to give preference to the more important service. In this case, fault-detector relays at each end of the channel are arranged to take control away from the telephone function if necessary. The remote-tripping function presents a different problem, since in this case no indication of the fault exists at all terminals; in fact, that is the exact reason the transferred tripping must be done. Since normally only one carrier frequency is used for both transmission and reception, the local receiver is desensitized by the strong signal produced by the local transmitter, and although it is possible to hear the incoming 3,000-cycle tone in the telephone receiver, it is of insufficient strength to operate the audio relay. Upon hearing this audio tone, it is a natural impulse for the operator to shut off his carrier transmitter since he assumes that the remote station is calling him, which of course is correct. As soon as the local transmitter is stopped, the audio signal increases greatly in volume and trips the circuit breaker. This difficulty could be avoided by using two different frequencies for transmission and reception, but this would necessitate changes in the method of protection on lines having more than two terminals and introduces certain other undesirable complexities.

There is no interference between the operation of the normal pilot relaying equipment and the remote-tripping equipment, since a transformer fault of sufficient magnitude to cause the pilot relays to operate will not require the assistance of the remote-tripping equipment.

Overmodulation of any carrier transmitter should be avoided since it produces harmonics in the audio as well as the radio frequency output. Accidental overmodulation of the transmitter at 1,500 cycles will produce some second harmonic output of the receiver at 3,000 cycles which may be sufficient to operate the tripping relay. To eliminate this possibility, the second audio-filter unit is tuned to 1,500 cycles and arranged to desensitize the 3,000-cycle filter when a 1,500-cycle tone is received. With the input adjusted for normal speech, the 1,500 cycles present in the telephone feature is insufficient to operate the 1,500-cycle relay.

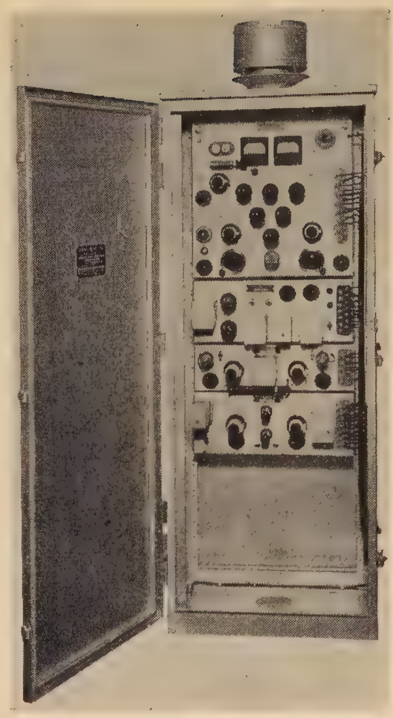

Figure 3. Carrier-current pilot relaying equipment, with oscillator and filter unit for transformer differential protection, in cabinet for outdoor mounting

Other Uses

In cases where there are three terminals of carrier on a line section, each set being equipped with remote-trip transmitter and receiver equipment, it will be found very useful to use a 1,500-cycle tone for telephone calling by using a simple code for each station. By this method the alarm bell will not operate at the third station while the other two stations are using the phone.

In cases where the remote circuit breaker is equipped with high-speed reclosing equipment, it is desirable that this feature should function only when the breaker trips for transmission-line faults. The relay connections should be arranged so that it is impossible for the automatic recloser to operate in any case where the circuit breaker is tripped by the transformer differential relay. After the faulty transformer has been removed from the circuit, a 1,500-cycle tone may be used to release the lockout thus restoring the breaker to normal operation.

Cases frequently occur where it is required to control a single remote circuit breaker or disconnect switch at an unattended substation. From the previous description it is evident that the remote-trip equipment can readily be adapted for this type of service, and by the addition of the proper auxiliaries an indication of the position of the remote device also can be obtained.

Installations

New system interconnections, where the tie points are at different operating voltages, and new loads tapped on existing lines have resulted in a considerable number of installations of this type of equipment. These applications have included the following arrangements:

A. Single circuit line with transformer at one end

B. Double circuit line with transformers at one end.

C. Single circuit three-terminal line with transformer at one terminal.

D. Single circuit three-terminal line with transformers at two terminals.

E. Single circuit three-terminal line with transformers at all three terminals.

Critical-Material Conservation in Induction-Motor Manufacture

HENRY M. HOBART
FELLOW AIEE

Present Importance of Avoiding Over-Motoring

IN several articles which recently have appeared in the technical press, it has been indicated that, in the interests of the war effort, standards of reliability in service may have to be compromised. As an example, one author advocates that electric motors should, in some cases, be utilized with continuous loads 25 per cent greater than has been considered good practice heretofore, "particularly if the ambient temperature within which the motors are expected to operate, is less than the recognized permissible maximum of 104 degrees Fahrenheit." That author adds the caution that this may not be practicable in all cases, since other factors than the continuous thermal rating sometimes determine the motor size. Then he adds: "However, in most war plants, such cases are relatively few."

Proposed Plan

In the present paper, careful consideration is given to the rating-up amounts which are desirable in the case of induction motors, in view of the acute urgency of conserving critical metals. The conserving of *copper* is of chief importance, but by the plan here presented there is effected at the same time a great saving of other metals. The plan recommended in this paper does not involve any compromise in "standards of reliability in service."

Predominating Importance of Induction Motors

Of all electric motors the induction motor is the one of which the greatest quantity is required by industry. If, for the total number of induction motors which will be manufactured in the year 1943, the per-horsepower consumption of copper and other metals can be reduced by 25 per cent, as compared with heretofore practice, and by plans not involving delays for redesigning and retooling, thousands of tons of copper and other metals will be saved and can be made available for direct military purposes. It is the intent of this paper to describe means for effecting this large saving without the occasioning of any delay in the production program of the manufacturers of induction motors.

The heretofore (and present) basis of rating of induction motors, so far as it relates to the limiting service temperatures, has been extremely conservative, as the result of a determination to ensure

1. Large margins of safety.
2. High service dependability.
3. Long life.

Furthermore, the basis was adopted a good many years ago, when the customarily used insulating materials of reasonable cost and ready application were considered inadequately heat resisting to withstand higher temperatures for long service periods. In recent years, wide experience has been gained with various insulating materials which will endure subjection to much higher temperatures for long periods of service. While usually these materials are more expensive than the early thermally inferior materials, several amongst them are manufactured in sheets, tapes, and all other required forms, so that they may be substituted without any redesigning of the motors, dimensional or otherwise. This substitution is an important feature of the conservation project described in the present paper.

Under the comfortable conditions of peacetime, no appreciable headway was made with rating-up proposals, notwithstanding the known availability of these better insulating materials. Now it is apparent that the conservation of copper and other metals is of such vital importance that all will co-operate eagerly in seeking for sound plans for the accomplishment. It is important to make the changeover with a minimum of delay. Any dimensional changes in the design and in the manufacturing specifications inevitably would interrupt smooth and rapid large-scale production and would be accompanied by disastrous delays.

Reduction of the Per-Horsepower Weight Achieved by Rating Up

Consequently it appears desirable that the *dimensions* of the present low-temperature—but nevertheless highly developed and excellent—designs shall be retained and that the reduction of the per-horsepower weight shall be effected by rating up the motors at present being manufactured. The only change in their construction will be the substitution of the high heat-resisting insulation but of precisely the same *dimensions* as the insulation heretofore used.

33 Per Cent Increased Rating

This plan will permit of increasing the rated output by 33 per cent, and reducing by 25 per cent the per-horsepower weight of copper and other metals required in the construction.

Precedents for Employing Higher Service Temperatures

Railway motors, totally enclosed motors, and the insulation of the rotors of turbogenerators long since have been standardized on the basis of much higher permissible service temperatures than those allowed for general-purpose induction motors. Contrary to predictions these three (and other) higher-temperature products have experienced no undue life impairment, when suitable heat-resisting insulations have been skillfully employed. Similar success will attend the use now proposed of higher service temperatures for the induction motors.

Present Severe Service Conditions Render High Stalling Loads Particularly Desirable

Let us now consider how the case stands with respect to limitations to rating up, other than thermal limitations. Leaving the motor design just "as is" (that is, on the present low-temperature basis), most sizes, particularly those for low speeds, will not have an adequately conservative stalling-load margin by the time their 33 per cent greater (new) rated loads are reached. Unless we do something about it, the margin between the (new) rated load and the original stalling load often will be too small, particularly for the rough and severe conditions of present-day production. The motor will stall at a

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HENRY M. HOBART is consulting engineer in Schenectady, N. Y.

lower load, the greater the drop in the supply-line voltage, and vice versa. It is important to realize that, with the high loading of power circuits now becoming usual, (and the necessarily associated great voltage drops) and with the intense incentive to accomplish tasks in the shortest possible time, there frequently will result the imposition upon the driving motors of much more severe voltage drops and momentary load peaks than are apt to be experienced in normal times. Evidently, while our rating-up plans will not encounter any difficulties as regards liability of the occurrence of breakdowns due to overheating, there is the need that there still shall be provided very liberal reserves of torque, over and above the torque corresponding to the increased rated load, so as to remove all likelihood of production interruptions from motor stalling. Such motor stalling would be more intolerable, in view of the emergency circumstances, than ever it could have been in peacetime.

Inherent Relations Between Temperature Limits Imposed, Stalling-Load Requirements, Periodicity, and Rated Speed

This matter of ensuring that the induction motor should possess a momentary stalling load liberally in excess of the rated load (while sometimes, particularly in the case of low-speed motors, it caused the motor designer to depart from otherwise best proportions) did not constitute a major problem, so long as the limiting service temperatures were required to be very low. That condition tended strongly (in almost all sizes and types), to make the permissible service temperature the limit of rated output. But now, the boot is on the other leg. Going over to the endorsement of higher service-temperature limits for induction motors brings us right up face to face with the condition that the momentary stalling load henceforth usually will constitute the actual limitation on the rated load for which a motor always can be relied upon to be liberally adequate in service. Fortunately, it has become recognized as good practice to employ for induction motors the highest practicable speed. For a given periodicity, the inherent stalling load (broadly speaking) is greater, the greater the rated speed. Obviously, this paper is concerned chiefly with the conditions in 60-cycle systems. For lower periodicities (such as 25 cycles), the provision of an adequately high stalling load rarely will impose a limit on the rating, except for extremely low-speed motors.

The stalling load can be increased by increasing the voltage at the motor's terminals. But the supply-circuit voltages have become so thoroughly standardized, and the motor designs have been standardized for so long a time for these particular supply-circuit voltages that, in order to provide at the motor terminals a voltage higher than the standard supply-circuit voltage, it has been necessary (1) to use special step-down transformers with secondary voltages higher than standard, or else (2) to boost the secondary voltage by interposing an induction regulator between the supply circuit and the terminals of the motor. But, while the higher terminal voltage will increase the motor's stalling load, it will impair both the power factor and the efficiency at light loads. A long time ago, the proposal was made (and used, to a certain extent), to provide a low terminal voltage at light loads and gradually higher voltages with increasing load (so as to provide the best characteristics at *all* loads) by equipping the transformer secondary with several taps and with means for transferring automatically the induction motor's terminals from tap to tap as the load increases. Tap-changing-under-load transformers could, of course, be used, the change being effected by an automatic regulator responsive to load changes. Feeder regulators, or special or complicated transformers, however, would take the gilt off the gingerbread and leave us using as much (or even more) per-horsepower weight of copper and other metals (for induction motor plus regulator) as for the original low-temperature (and high-copper) motors with their high reserves of stalling torque.

Desirability of Retaining (At Any Rate, for the Duration) The Present Excellent Designs of Induction Motors

Actually, it is the volts per turn which we desire to increase, in order to increase the stalling torque. The simplest way to increase the volts per turn obviously is to decrease the number of turns and increase the magnetic flux. This, however, means new designs and the inevitable great delays for development and testing. (*Even then* there will be incurred a certain amount of impairment of the motor's light-load characteristics.) Furthermore, after years of continual improvements effected in the course of many redesignings, most induction-motor manufacturers (on the basis of the standardized low-temperature limits) have arrived at lines of very well balanced designs in which the polar pitch, and the core length, and the volts per turn for each particular rated output and speed and voltage and periodicity are so nicely balanced as to provide about the best practicable combination of high power factor, efficiency, starting torque, and reasonably low starting current.

Clearly, it is of prime importance to salvage these many fine designs (at any rate, for the duration) and thus also have the great advantage of eliminating the enormous and time-consuming task of developing new lines of induction motors to fit the higher permissible service temperatures and decrease the per-horsepower weight of copper and other metals. That is not a task which possibly could be accomplished in a few weeks (or even in less than a good many months), and also it would be likely to require considerable retooling. For each of the usual horsepower sizes there are required designs for various voltages and speeds and degrees of enclosure and other subclassifications. A fresh start would require, in this case, the laborious and time-consuming development and testing of many dozens of new designs of induction motors.

Thus it appears that an effective plan, not involving any delays, for reducing the per-horsepower weight of copper and other metals required in induction-motor manufacture is to continue building the present induction motors without any changes except that which has been indicated, namely, the use of the better insulations with high heat-resisting properties. The plan requires the provision of some *external* means, or some *externally* located material, for increasing considerably the stalling torque so that it shall be liberally adequate for the conditions of the increased rating, and for the more intense and strenuous conditions of service consequent upon the war effort. Especially in war plants the certainty of freedom from even the slightest service interruptions is of vital importance. Already it has been explained that, for this requirement of increased stalling torque, it will be necessary to provide some means whereby any instantaneous or sustained overloads shall be accompanied by an adequate increase in the volts per turn. Furthermore, it will be desirable that the means employed shall embody features contributing toward improving the motor's light-load characterisics by *decreasing* the volts per turn at light loads.

Use of a Hookup With Series and Shunt Capacitors

As an effective means for providing that the volts per turn shall automatically

increase with increasing load, there may be employed a plan which was conceived 21 years ago. At that time, there was no urgent need for providing induction motors with this feature of variable volts per turn. Also in other respects, the proposal was regarded as premature. But the present greatly changed conditions indicate the method to be particularly timely and appropriate. The following is a description of it.

Description

Three series capacitors are interposed between the three-phase supply circuit and the three terminals of the windings of the induction motor. In addition, there are provided across the motor terminals three shunt capacitors. Either the series capacitors are designed for a current equal to the current in the motor's windings or they may be located in the secondaries of three small transformers whose primaries are interposed in series with the three motor terminals.

The shunt capacitors are proportioned for a current equal to the lagging component of the motor current at its rated load (that is, they are proportioned for complete compensation for unity power factor at rated load). Consequently the power factor is leading for all loads from no-load up to the rated load. (So far as concerns the employment of shunt capacitors with induction motors, this already is widely practiced, and it so improves the economics of the supply system as usually to amply justify the outlay for the required shunt capacitors.)

The Purpose of the Series Capacitors

For the purposes of the project described in this paper, the series capacitors are so proportioned as to decrease and increase the motor's terminal voltage by desired amounts below and above the constant voltage of the supply circuit. For loads less than some selected value (rated load, in the case being considered), the lagging current into the motor is overcompensated by the leading current in the shunt capacitors. The resultant current from the supply circuit, being a leading current, causes the motor's terminal voltage to be less than the supply circuit's voltage, because of the voltage drop which is occasioned in a capacitor when a leading current flows through it. From rated load upward, the opposite result is obtained, and the motor's terminal voltage increases as the load increases, because the current through the series capacitors is lagging.

Illustrative Example

As an example, let there be considered the case of a three-phase 60-cycle 4-pole 480-volt squirrel-cage general-purpose induction motor, which, for the heretofore usual low-temperature practice, is rated at 75 horsepower. At its rated voltage of 480 volts, such a motor will carry double load (that is, 150 horsepower) without stalling. By providing this 75-horsepower motor with the capacitor hookup recommended in this paper, it may be rated up to 100 horsepower and will then, without stalling, carry momentary overloads up to 200 horsepower. Of the three series capacitors for this purpose, each will have such capacitance as to increase the motor's terminal voltage from the supply circuit's constant voltage of 480 volts up to 555 volts when the load has increased from rated load of 100 horsepower, up to twice the rated load namely, 200 horsepower. Conversely, these series capacitors serve to decrease the motor's terminal voltage down to values below 480 volts, to an extent dependent upon the amount by which the load decreases below the rated load.

Aggregate Improvements Thus Provided

This decrease of the voltage at low loads and its increase at high loads also serve to increase the low-load and high-load efficiencies above the values which can be realized with constant voltage maintained at the motor terminals for all loads. Not only are the improvements resulting from these various effects, accompanied by increased power factor and efficiency at all loads, but also they endow the (motor plus capacitors) combination with a leading characteristic for all loads up to the rated load, and they afford a means for increasing the stalling load above that attainable in a constant-voltage design and consequently also a means for increasing the rated output. Finally, there is thereby effected an important decrease in the per-horsepower weight of critical material required in the motor's construction.

Rating of Required Capacitors

It has been stated already that the provision of shunt capacitors for obtaining power-factor improvement long since has been admitted quite generally to justify their cost. The economies resulting therefrom extend throughout the entire system over which the induction motors are supplied and relate both to reduced capital outlays and reduced losses in all the material interposed between the source and the induction motors supplied therethrough. The further-mentioned advantages of greater motor efficiency at all loads, increased rated output, and decreased per-horsepower weight of critical metals undoubtedly will be agreed to justify the outlay for the series capacitors. In the cited example of a 100-horsepower motor with a stalling load of not less than 200 horsepower, the required amount of series capacitance comes to 30 kva, provided in three 10-kva capacitors interposed betweeen the supply-circuit terminals and the motor terminals. These series capacitors boost the system voltage from 480 volts at a load of 100 horsepower, up to 555 volts at a load of 200 horsepower, the currents per phase for these two loads being respectively 100 amperes and 220 amperes.

Series-Capacitor Compounding Transformer

When, in the interests of decreasing the losses in the distribution circuit, load-center location close to the induction motor is provided for the step-down transformer (or group of transformers), it will be practicable, often, to decrease the outlay for the series capacitors by locating them in the high-voltage side of the step-down transformers. Thus, for the example chosen (a 480-volt induction motor), and a primary voltage of 4,800 volts, the voltage stress across each series capacitor would then be much more favorable for the design of the series capacitors with respect to decreasing their cost. This plan amounts to providing *compounding* transformers. For the particular case of the illustrative example, the secondary voltage of the (nominally) 480-volt transformer would increase with the load from a value of, say, 415 volts at no load of the induction motor up to 555 volts just below the occurrence of the motor's stalling load.

Development of Power Capacitors

In the period from 1919 to 1922, when this project and other *Series*-capacitor power projects were proposed (although *shunt* capacitors already were sufficiently common in power applications to be quite familiar to power engineers), the idea of a *series* use of capacitors with *power* apparatus was considered by many power engineers to be of questionable soundness and in need of demonstration. Another objection was that power-capacitor manufacture was then only in its

early beginnings. Paper of the desired quality and with the necessary freedom from objectionable constituents remained to be developed. The paper needed to be of extreme thinness so that weaknesses at various places in its extent could be averaged out by the super position of several paper layers between adjacent conducting foils. Processes of evacuation and impregnation still left much to be desired. The demand for capacitors for power applications was out of all proportion too small to enable the cost to be satisfactorily decreased by large-scale mass production. Furthermore, those were days of extravagance and waste. The conditions were the reverse of propitious for attracting interest to a proposal which was regarded as both premature and unimportant. That, admittedly, then was the case with this proposition.

In 1924, however, presumably out of a spirit of amiable tolerance toward persistent and reprehensible importunacy, the scheme was made the subject of a patent application, and patent 1,595,937 of 1926 finally emerged. It is probable that the method never has been employed in service. The present emergency circumstances and the timely advantages which the project provides would seem to be providentially opportune.

Summary

There follows a list of some of the attendant circumstances and of the advantages which may be gained by the use of the project:

1. Thoroughly reliable power capacitors now are manufactured in great quantities by many firms.

2. Cost is much lower than in 1922.

3. By simple constructions embodying the external-pressure principles by means of which Hochstadter has demonstrated that the service insulation strength of underground high-voltage cables can be doubled for a given insulation thickness, the per-kva cost of power capacitors readily may be decreased much further.

4. By the use of power capacitors as a simple adjunct the power factor and efficiency of an induction motor may be considerably improved for all loads, light and heavy, and the stalling load may be much increased. Throughout a considerable range of load, system economies associated with a leading current will be obtained.

5. Higher-temperature service operation now is recommended, and the rating of the present highly-developed, low-temperature, induction motors may be increased by 33 per cent, without requiring any delays, since no re-designing is involved. There is required merely the substitution of the high heat-resisting insulation now available.

6. The project will permit of the conservation of considerable quantities of critical materials as the result of decreasing by 25 per cent, the per-horsepower weight of the motors.

The recommendations made in this paper (and the means proposed) for increasing by 33 per cent the present service rating of general-purpose induction motors is more conservative than many will realize. It is anticipated that, when the electrical industry has acquired more confidence in the adequacy of the available better insulations (as regards withstanding heat deterioration), practice will advance to the extent of permitting even higher service temperatures and securing further reductions in the per-horsepower weights of copper and other metals which it is of such critical importance to conserve. But rather than jeopardize the acceptance of his recommendations, the author has refrained from advocating still greater amounts of service-rating increase.

WPB L-221

In a note on page 81 of the February 1943 issue of Electrical Engineering, the War Production Board's general conservation order L-221 is stated to contain a provision whereby the horsepower of a new electric motor shall not exceed the requirements of the specific job for which the motor is purchased. Compliance with this and other conservation provisions contained in the order (so far as it relates to general-purpose motors) are expected to conserve annually about 6,000 metric tons of copper and 55,000 metric tons of carbon steel. In the present paper attention has been confined to the specific case of general-purpose induction motors, and means particularly applicable thereto are recommended for effecting savings of amounts of critical materials which should exceed the amounts envisaged in WPB L-221. These recommended means also will improve the operating conditions as respects efficiencies, power factors, stalling loads, and reduced rate of life impairment.

Ambient Conditioning

In conclusion, attention well may be called to the present advanced state of development of systems for the conditioning of factory and workshop premises. While consideration for the health and efficiency of the personnel quite rightly is the main objective, it is not out of place to consider also the extent to which the rate of life impairment of the *motors* may be decreased by decreasing the ambient temperature in the midst of which the motors' existences are spent. The 68 degrees Fahrenheit which is beneficial for the personnel should be capable of being closely approached, even in the immediate surroundings of the motors, rather than any approach to the temperature of 104 degrees Fahrenheit now assumed in the rating regulations as being a temperature of likely occasional occurrence. It will, of course, be necessary to take into account the fact of the relatively great amounts of heat being developed in the motors as a consequence of their losses. But by simple applications of fundamental principles of local air circulation, accentuated when desirable by surface vaporization methods of heat transfer, the desired 68 degrees Fahrenheit temperature should be capable of close approach at all seasons and localities and even at distances of relatively few inches from the external surfaces of the motors.

A New High-Speed Balanced-Current Relay

V. N. STEWART
ASSOCIATE AIEE

NEED for a high-speed, balanced-relay current which will not function when the first half-cycle current is deficient in one of the two parallel circuits being protected, has been shown by the recent paper "The Effect of Current-Transformer Residual Magnetism on Balanced Current or Differential Relays."[1] This is the result of high residual magnetism of the current transformer caused principally by the d-c component of an offset wave on previous short circuits.

A high-speed, induction-cylinder-type relay has been developed in which the operating-coil circuit has an inherent delay of approximately one cycle, due to the time constant of a resonant circuit. The restraining torque of the relay is effective in a small fraction of a cycle and is obtained by using a flux-shifting copper tube acting as a lag ring or short-circuited secondary.

Application

Several AIEE papers[2,3,4] have presented the development and application of high-speed relays in which the speed of operation was recognized as an essential characteristic in obtaining greater system transient stability. Since the standard time for circuit-breaker operation was reduced to eight cycles or less, this meant that relay operating times of approximately one or two cycles were necessary to realize the full benefit of this circuit-breaker operation. In designing a new relay, one must recognize the increasing demand for high speed, but its attainment must not be accompanied by sacrifice of either selectivity or reliability.

The balanced-current relay is most generally used to provide protection against current unbalance at the source end of parallel untapped transmission lines, where the unbalancing is occasioned by phase-to-phase or ground faults in either line. It may also be used for a similar protection at the receiving end of parallel lines provided there is an additional source of power supply (other tie lines, generators, or synchronous apparatus) at the receiving end which will increase the fault current in the defective line at least 10 to 25 per cent. The schematic one-line diagram, Figure 1, illustrates this application and emphasizes its limitation for the load end protection where the currents in breakers *A* and *B* would be equal.

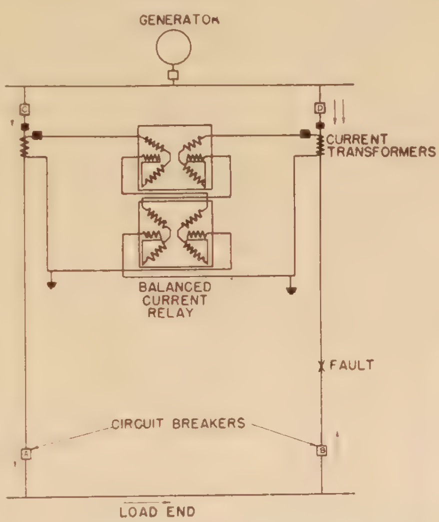

Figure 1. Schematic one-line diagram illustrating balanced-current protection of parallel lines

Operating requirements for the new relay cannot be reduced from that followed on existing high-speed, balanced-current relays. This means that satisfactory protection for simultaneous faults on the parallel lines, protection on single-line operation, high-speed auxiliary switches or interlocking relays, operation with bus tie breaker, proper functioning during magnetizing inrush, and current-transformer characteristics are items for consideration. Besides these factors tending toward faulty operation, if improperly handled, there have been recorded occasional false trippings which, until recently, were without satisfactory explanation.[1,5] Now, since it has been shown that residual magnetism in the current transformer may result in false operations with existing high-speed relays, a relay has been designed to function correctly although this undesirable, yet unavoidable, current-transformer characteristic exists.

Description and Operation of Relay

The relay unit with eight-pole, induction-cylinder construction previously presented before the Institute[6] is inherently a high-speed device and readily lends itself to balanced-current operation. The available poles permit both the operating and restraining functions to be accomplished in a single unit. The single relay unit protects only one phase of one line and the relaying of both lines is accomplished with a two-element relay shown in Figure 2. For complete balanced-current protection at the generating end of two parallel lines, three relays are sufficient; two for phase-to-phase faults and one for ground faults.

Six of the available eight poles are used, and their location is shown in Figure 3. The left three coils, denoted by *A* and *B*, are the operating coils while the right three coils, *C* and *D*, provide the restraining torque. These torque-producing elements exert their force on a common rotor in opposition to one another to give a balance. The production of this torque is like that of a wattmetric

Figure 2. Single-phase high-speed balanced-current relay

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V. N. STEWART is in the relay engineering department, General Electric Company, Philadelphia, Pa.

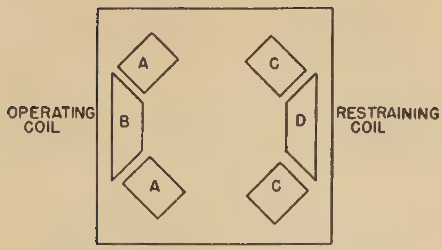


Figure 3. Coil locations in a single element of the balanced-current relay

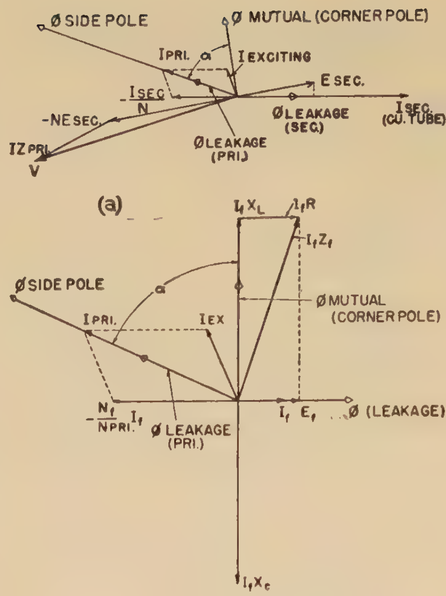


Figure 4. Vector diagrams showing flux relations

(*a*). Restraining circuit with copper tube

(*b*). The operation circuit with the floating circuit

Subscript *f* denotes floating circuit (secondary)

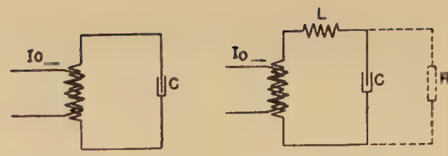


Figure 5. Diagram showing two forms of floating circuits

element, and in the case of Figure 3, the coils B and D produce rotor currents which react with the flux of their adjacent coils. The torque magnitude is a function of the product of the flux and the sine of the angle between them. From this relation and the connections of Figure 1 it is obvious that some means must be provided to obtain a phase displacement between the pole flux for both the operating and restraining circuits.

For the restraining circuit a flux-shifting copper tube is employed. This method is similar to one used in current-polarized directional units of the induction-cylinder construction. The vector diagram, Figure 4*A*, gives an approximate time-phase relation for the restraint circuit showing the flux shift in the corner poles (C of Figure 3).

For the operating function, the necessary flux shift is obtained by means of a floating circuit. The purpose of this flux-shifting method is to permit the use of a tuned circuit in which the delayed current build-up retards the action and the operating torque. As can be observed from Figure 6, the impetus imparted to the relay by the difference current occurs during part of the first cycle depending on the nature of the fault, and the delay required does not detract from the high speed (Figure 8) of the relay.

The floating circuit is composed of a many-turn coil to replace the copper tube and tuned with a capacitor mounted inside the relay case. The circuit constants can be modified with the use of a reactor, resistor, or both, as shown in Figure 5, to control the magnitude and time. For this condition, Steinmetz[7] has shown that the current in a series a-c circuit where $R^2 < 4L/C$ is equal to

$$i=\frac{E}{Z_0}\cos(\theta-\theta_0-\gamma)-\frac{E}{Z_0}\epsilon^{-\frac{R}{2x}\theta}\left\{\cos(\theta_0+\gamma)\cos\frac{q}{2x}\theta+\left[\frac{2x_c}{q}\sin(\theta_0+\gamma)-\frac{R}{q}\cos(\theta_0+\gamma)\right]\sin\frac{q}{2x}\theta\right\}+\epsilon^{-\frac{R}{2x}\theta}\left\{i_0\cos\frac{q}{2x}\theta-\frac{2e_0+Ri_0}{q}\sin\frac{q}{2x}\theta\right\}$$

Where

E = maximum impressed voltage
R = resistance
x = inductive reactance
x_c = capacitive reactance
$Z_0=\sqrt{R^2+(x-x_c)^2}$
$\theta=2\pi ft$
t = time
f = frequency
$\gamma=\tan^{-1}\frac{x-x_c}{R}$
$q=\sqrt{4xx_c-R^2}$
θ_0, i_0, and e_0 = values at time equals zero

This equation consists of three parts:

1. The permanent term which is remaining after the transient is completed.
2. The transient term which is dependent on the circuit constant.
3. The third term depending on the initial electrical conditions. This last term disappears if the circuit is dead at the start.

A vector analysis (Figure 4*B*) for the floating circuit represents the flux relation during the steady-state operation. The angle α represents the phase displacement between the flux in coils C and D, and the torque is a function of the sine of this angle.

The oscillogram (Figure 6A) shows the current build-up in this circuit, and the magnitude for the first half cycle is only a fraction of its maximum steady-state condition. This leaves the operating circuit deficient in torque whereas the restraint build-up is immediate and has a restraining bias even though its current supply from the current transformer is affected by the residual magnetism.

Relay Characteristics

In reference to Figure 1 it will be noted that the operating coil of each unit is connected in series with the restraining coil of the other unit. Therefore, the fault current of one line which operates one of the units increases the restraint in the other, thus providing a selective operation. Figure 7 gives typical operating characteristics for both ground and phase relays with a 125 per cent slope. This slope has been used in the application of balanced-current relays to assure sufficient margin for the inequalities of current-transformer characteristics, line characteristics, and other factors af-

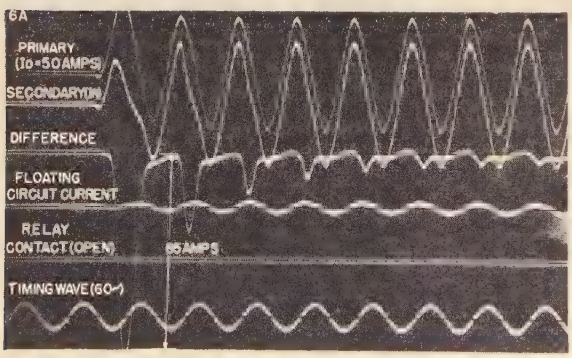
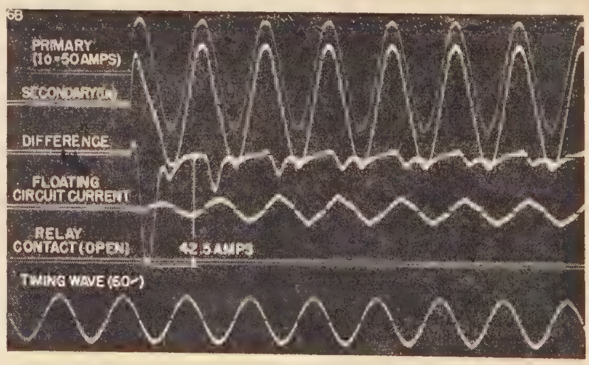


Figure 6. Oscillograms showing operation of relay during difference currents caused by residual magnetism. Two fault conditions are represented

(*A*). With an offset wave

(*B*). Symmetrical current wave



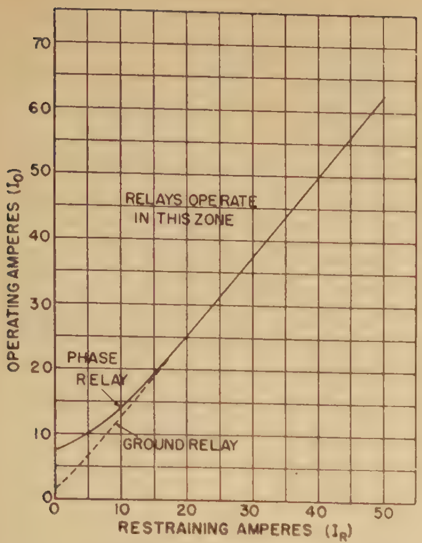


Figure 7. Operating characteristic of phase and ground balanced-current relays

fecting the current balance. When these items are corrected or compensated, it should be satisfactory to use a relay functioning with a slope of 110 per cent.

The high-speed time characteristic is indicated in Figure 8 for this relay.

Tests

The testing program was composed of two parts:

1. A relay having all the characteristics presented in the forepart of this paper had to be developed and tested.

2. This same relay had to be subjected to operating conditions which equaled those found in the field. This part was conducted as a continuation of the work already covered on the current transformers and presented before the Institute.[1]

Typical oscillograms were taken using the test circuit of Figure 9, and these are reproduced in Figure 6 to show the instantaneous relation between the circuit characteristics as they affect the relay. The traces indicate current-transformer primary and secondary currents, the

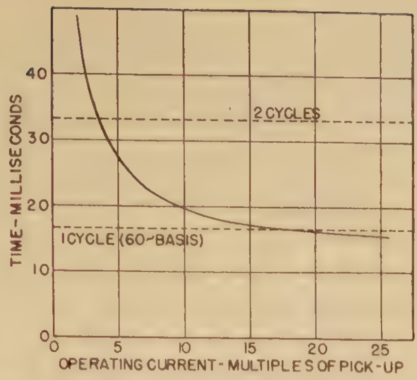


Figure 8. Operating time of high-speed balanced-current relay at zero, 5 and 20 amperes restraint

difference (or error) current, the operating-circuit build-up, the relay contacts, and the timing wave. With a 1/1 ratio current-transformer tests were made with the primary and secondary windings each connected in series with an oscillograph shunt and vibrator. Then the difference between the drop in the two shunts gives an instantaneous value which is a function of the difference current, and it is recorded by a third vibrator. Likewise, the instantaneous current magnitude of the floating circuit was recorded with the use of another oscillograph shunt and vibrator by a means which minimized its effect on the circuit's transient characteristic.

Conclusions

A balanced-current relay has been described which gives fast, sensitive protection comparable to that previously available yet faultless in operation when energized from current transformers previously saturated and containing residual magnetism. This has been accomplished at the expense of a slight delay of approximately one cycle which is easily tolerated. This has resulted from the application of the floating-circuit build-up to override the error current of the first cycle. Also, it can be expected that this feature will be useful in the design of other high-speed relays.

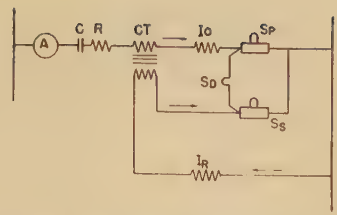


Figure 9. Test circuit used for checking relay operation and for oscillographic measurement of difference current

A—Ammeter
C—Synchronous switch
CT—Current transformer (ratio one to one)
I_o—Relay operating coil
I_R—Relay restraint coil
R—Reactor or load box
S_D—Oscillograph element to record difference current
S_P and S_D—Shunts with oscillograph elements; primary and secondary currents respectively

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A Study of Voltage Transients in Arc-Furnace Circuits

J. B. HODTUM
MEMBER AIEE

J. B. RICE
ASSOCIATE AIEE

IN the history of the operation of arc furnaces for melting steel there have been occasional evidences of overvoltage transients. These evidences have been chiefly electrical breakdown of equipment associated with arc furnaces which were not attributable to causes other than abnormally high voltage. In the last three years with the increased number of arc-furnace installations and the increased seriousness of interruptions to production, a considerable amount of attention has been focused on this problem. This paper presents the results of voltage surge measurements made on two furnace installations in an attempt to accumulate accurate data as to the magnitude of such overvoltages and the factors which influence them. Before entering into a discussion of voltage surges, however, it seems advisable to review the component electrical parts of an arc-furnace circuit and the procedure followed in its operation.

Furnace Circuits and Their Operation

Figure 1*a* is a simplified diagram showing the parts of a typical arc-furnace circuit, and Figure 1*b* is the corresponding schematic single-line diagram. In all but very large installations the separate reactor *R* is necessary because the inherent reactance of the circuit is not sufficient to produce over-all circuit stability so that an arc can be maintained. The vertical positions of the carbon electrodes in the furnace are individually and automatically controlled so as to maintain as nearly as possible constant current in each electrode. This regulating means is not shown in Figure 1. However, as will be apparent later, the characteristics of such control may have an important bearing upon voltage surges caused by instability of the arc, particularly during the melt-down period when the arc is most unstable. A companion paper[1] describes a new electrode control which it is believed has the advantage of increasing the stability of the arc.

After the furnace has been charged usually with steel scrap, the operator closes the circuit breaker which energizes the furnace transformer. The carbon electrodes are still clear of the charge, and therefore there is no load on the transformer. The operator now lowers the electrodes until they come in contact with the charge and an arc is initiated. The automatic control then takes over and attempts to regulate the electrodes so as to maintain constant current. However, in the initial part of the heat, before a considerable portion of the charge has been melted, it is not possible to maintain constant current, and the load fluctuates quite wildly between short circuit when the electrodes are in contact with metal and virtual open circuit when the arc may be completely extinguished or at least reduced to a very low current.

During this melt-down period the furnace transformer is operated in the upper range of the available secondary voltages with a correspondingly high value of reactance if a separate tapped reactor is used.

After the charge is completely or mostly melted, the operator changes taps on the transformer to obtain a lower secondary voltage and usually a lower reactance, so as to supply power to the furnace at a reduced rate. In doing this the operator must open the circuit breaker to deenergize the transformer. The melt is then continued for perhaps an hour to an hour and a half until such time as it becomes advisable to further reduce the rate of power input to the furnace. At this time the operator again changes taps on the transformer to obtain a still lower secondary voltage, and usually all the reactance other than that inherent in the transformer is cut out.

Each time that the operator changes taps on the transformer, it must be deenergized by opening the circuit breaker. In doing this there are two alternatives. The electrodes may be first raised so as to extinguish the arc before opening the breaker. In this case the breaker interrupts transformer magnetizing current only. As an alternative the breaker may be tripped without raising the electrodes so that it interrupts load current as well as magnetizing current. It has become fairly standard practice to follow the procedure of raising the electrodes before tripping the breaker, partly at least because it has been the concensus of opinion that this would result in less probability of high-voltage transients and also in reduced duty on the breaker.

The length of time required to make a complete heat of steel varies considerably depending upon the size of the furnace and the kind of steel being made. However, a typical figure may be taken as three hours. During this time according to the procedure outlined, the breaker will be closed and opened three times in order to start the heat and change taps. On the average there will be perhaps two or three additional switching operations for various reasons, such as slagging off, lengthening electrodes, and so forth. A total of six switch closings and six trippings per heat is perhaps a reasonable figure. Many installations are at present operating 24 hours per day, and with an average length of heat of three hours, this would mean approximately 40 to 50 switch operations per day. Some instal-

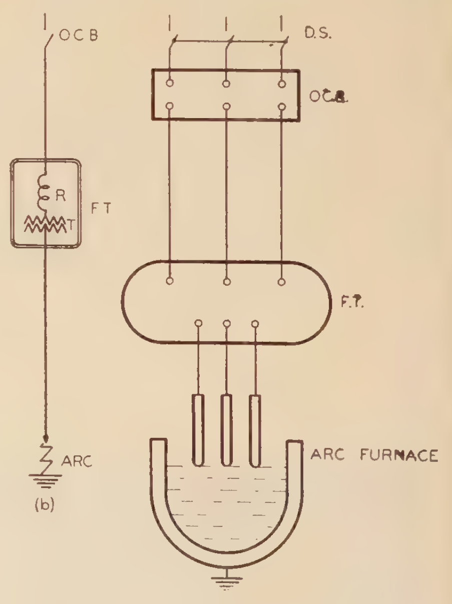

Figure 1. Typical arc-furnace circuit

DS—Disconnect switch
OCB—Oil circuit breaker
FT—Furnace transformer with self-contained reactor

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J. B. Hodtum and J. B. Rice are, respectively, development engineer and assistant development engineer for the Allis-Chalmers Manufacturing Company, Pittsburgh, Pa.

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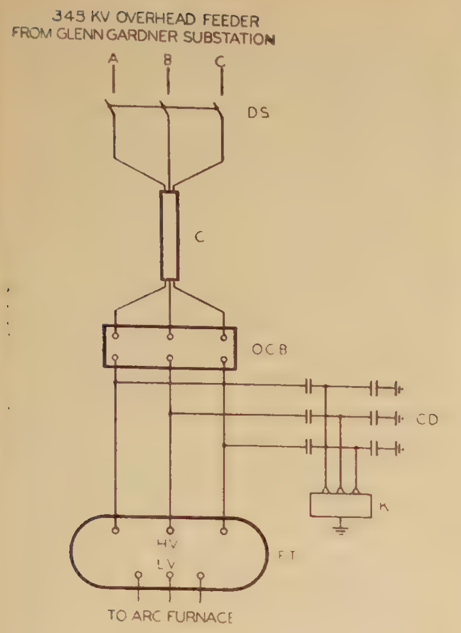


Figure 2. Furnace circuit at location *A*

DS—Disconnect switch
C—34.5-kv three-conductor type *H* underground cable 650 feet long
OCB—Oil circuit breaker
FT—Furnace transformer with self-contained reactor
CD—Capacitance potential divider
K—Klydonograph

lations have as many as 100 switch operations per day.

Possible Causes of Voltage Surges

Following the preceding discussion of furnace operation we may list the following possible causes of voltage transients in arc-furnace circuits:

(*a*). Instability of the arc.

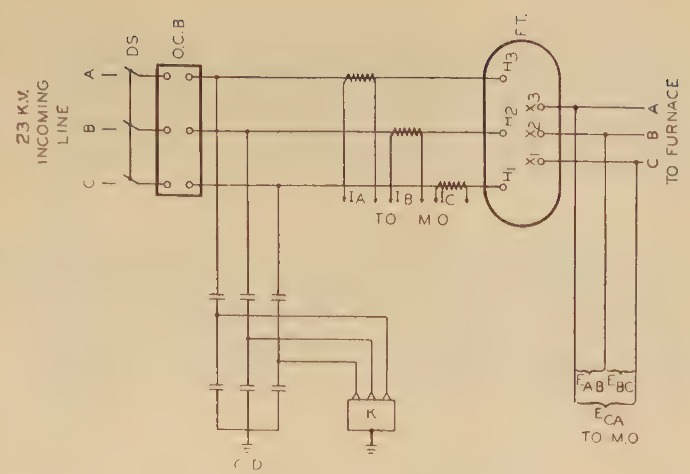
(*b*). Raising or lowering the electrodes to interrupt or start the arc.

(*c*). Switching.

The role played by intermittent arcing in the production of high-voltage transients on transmission systems is well known. It seems logical therefore to expect that a similar phenomenon might be found in circuits in which the load itself is an arc. If this is true, voltage surges should be particularly noticeable during the melt-down period when the arc is most unstable. Furthermore, it is the authors' opinion that the systems supplying most arc furnaces are so large that they offer very low impedance to surges originating on the low-voltage side of the transformer and therefore tend to suppress the transmission of such transients to the high-voltage side of the transformer. This being the case, voltage transients caused by arc instability should be observable, principally, on the secondary side of the transformer. At one of the two installations tested, special steps which are described later were taken to detect the presence of overvoltages on the high-voltage side of the transformer during furnace operation, and it was found definitely that no appreciable overvoltages occurred. On the other installation, no special steps were taken to detect surges on the high-voltage side during furnace operation, but an examination of Klydonograph film records of regular heats indicates, although not conclusively, that overvoltages were not obtained on the primary side except when switching occurred. No tests were made on the low-voltage side of the furnace transformer.

Figure 3. Circuit used for making tests at location *B* without potential transformers in the circuit. Secondary transformer voltages recorded by the magnetic oscillograph

DS—Disconnect switch
OCB—Oil circuit breaker
FT Furnace transformer with self-contained reactor
CD—Capacitance potential divider
K—Klydonograph
MO—Magnetic oscillograph



Since it is common practice to raise the electrodes before tripping the breaker and to lower the electrodes after the breaker is closed, it is possible that the interruption and initiation of the arc by this means might result in voltage surges. At one of the installations tested by a procedure described later, it was definitely determined that overvoltages were not obtained on the high-voltage side of the transformer when the electrodes were raised or lowered to terminate or strike the arc in the furnace. This test was not made at the other installation.

It is well known that the operation of switches in electrical circuits may cause voltage surges and that the magnitude of these surges depends upon the characteristics of the circuit and of the switch. Furnace circuits are not essentially different from other power circuits, except for the fact that the load has the peculiar characteristics of an arc. It might be expected, therefore, if there is any difference between the character of switching surges obtained on furnace circuits from those obtained on other power circuits that it would be observed when switching under load. It is advisable, therefore, to consider separately the conditions which exist when switching under load and when switching no-load, and also when closing and opening the circuit.

Although closing the circuit breaker under load is probably not practical for

Table I. Comparison of Two Installations Tested

	Location A	Location B
Primary voltage	34.5 kv	24 kilovolts
Frequency	60 cycles	60 cycles
Transformer (secondary delta connected)	3,000 kva, 3 phase	3,000 kva, 3 phase
Delta to wye primary switch	Internal	Internal
Secondary voltage range with		
Primary winding in delta	220–140 volts	235–160 volts
Primary winding in wye	127–81 volts	136–93 volts
Reactor	Internal, 21 per cent	Internal, 20 per cent
Oil-circuit-breaker current rating	600 amperes	600 amperes
Oil-circuit-breaker interrupting rating	500,000 kva	500,000 kva
Oil-circuit-breaker insulation	46 kv	34.5 kv
System grounding	Petersen coil—26 miles away	Solid—4 miles away
Miles of connected high-voltage line	350	4
Underground cable	650 feet, type *H*, 3*C*, 34.5 kv	None
Lightning arresters	Oxide film	Porous block—25 kv
Distance from lightning arrester to furnace	700 feet (source side of cable)	50 feet (end of line)
Instruments used in tests	Klydonograph	Klydonograph Cathode-ray oscillograph Magnetic oscillograph
Circuit diagram showing instruments	Figure 2	Figures 3 and 4
Secondary grounding	Lamps	Lamps

standard operating procedure, a few tests were made as a matter of interest and are reported under "Test Results." As might be expected, no overvoltages resulted at the transformer high-voltage terminals although current inrushes were obtained.

No-Load Versus Load Tripping

There has been much discussion with reference to the relative merits of switching out under load or with load on the transformer. In other words, should the operator raise the electrodes to extinguish the arc before he trips out the breaker, or should he not? It has been pointed out by some that breaking transformer magnetizing current may cause a voltage surge because of the inductive kick produced by the sudden collapse of the stored magnetic field in the transformer core. However, it must be remembered that the transformer core is a closed magnetic circuit and therefore will retain permanently a residual magnetization which is usually in the order of 60 per cent of the maximum induction. Furthermore, if the

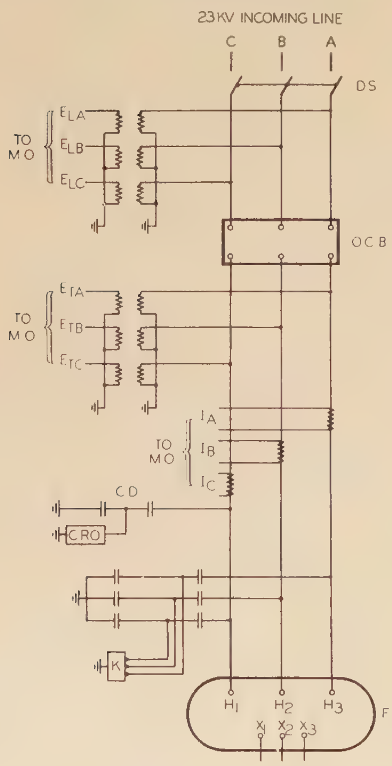


Figure 4. Circuit used in making tests at location *B* in which potential transformers were used. Primary transformer voltages recorded by the magnetic oscillograph

DS—Disconnect switch
OCB—Oil circuit breaker
FT—Furnace transformer with self-contained reactor
CD—Capacitance potential divider
K—Klydonograph
MO—Magnetic oscillograph
CRO—Cathode-ray oscillograph

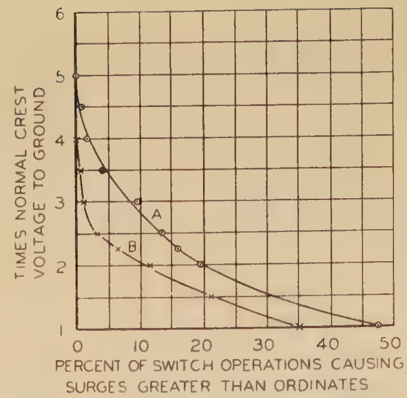


Figure 5. Percentage distribution curves of Klydonograph surge records

Curve *A*—Location *A*
Curve *B*—Location *B*

current is interrupted in the breaker at normal current zero, without appreciable distortion of the current wave, there will be no inductive kick at all because both flux and current will decrease along their normal paths to their steady-state value corresponding to current zero. In most modern breakers this is usually very close to what actually happens, because such breakers do not greatly tend to force current zero. Although breaking transformer exciting current can undoubtedly produce inductive kicks of two to three times normal (some of the film records obtained show such inductive kicks) it seems unlikely that overvoltages caused by breaking magnetizing current should be a source of serious concern.

Those who have advocated no-load tripping as preferable to tripping under load point out that interrupting load current may be dangerous because if the arcs in the circuit breaker and furnace are simultaneously extinguished at load-current zero, a large amount of stored energy may be trapped in the magnetic field of the transformer core. This is because the power factor of the load may be relatively high (85 or 90 per cent) while the power factor of the magnetizing current will be very low (in the order of 20 or 30 per cent). This means that both primary and secondary circuits may be opened when the flux in the core is near its maximum. The collapse of this flux would then tend to produce a very high inductive kick. However, it must be remembered that the arc gap in the furnace is relatively short and the space in the arc path is usually hot and retains its ionization for some length of time. The arc path may therefore offer a protective discharge path which will break down at a very low voltage. This is particularly true in the latter parts of the heat. During the melt-down period, however, when the arc is still comparatively long and when the arc space cools rapidly upon extinction of the arc, the trapping of magnetic flux in the core as has been described is believed to be a possible cause of overvoltages which may produce insulation failures. In considering this it should be remembered that the voltage required to break down the arc in the furnace will be multiplied on the high-voltage side of the transformer by the turn ratio. In the tests a number of surges of several times normal (up to approximately four times normal) were obtained because of tripping under load, chiefly in the early parts of heats. On the other hand, quite a few load trippings made after the charge was melted down produced no appreciable surges.

Summing up, it is believed that tripping

Table II. Summary of Test Results

	Location A	Location B
Approximate total number of switch operations (close or trip)	1,400	1,000
Approximate total number of surges recorded on Klydonograph	650	200
Approximate number of heats recorded on Klydonograph	70	50
Total number of surges recorded on cathode ray oscillograph	None	110
Total number of surges recorded on magnetic oscillograph	None	150
Maximum switching surge—times normal	5.2	3.8
Surges unidirectional or oscillatory	Unidirectional	Both
Polarity of Klydonograph surge figures	Positive and negative equal	Mostly positive
Surges on primary circuit caused by load	No	Probably not
Surges caused by tripping or closing breaker	Closing chiefly	Both equally
Surges caused by raising or lowering electrodes	No	No test
Effect on switching surges of		
Transformer tap position	Not important	Not important
Supply-system grounding	See Figure 6	No test
Underground cable ahead of breaker	See Figure 6	No cable
Natural frequencies of switching transients		
No-load tripping	No test	6,500 to 10,000
Load tripping	No test	5,000
No-load closing	No test	6,000 to 9,500
Load closing	No test	6,200
Percentage distribution curve of switching surges	Figure 5	Figure 5

under load in the latter parts of the heat with a short hot arc may cause lower overvoltages than tripping at no-load, but tripping under load in the early parts of the heat before melting down is completed definitely should be avoided.

Circuit Data

Tests were made on two installations identified in this discussion as location *A* and location *B*. The pertinent information on the furnace circuits and the power systems supplying them is presented in tabular form in Table I. It will be noticed that the chief difference between location *A* and location *B* lies in the supply system. The furnace circuits including the transformers and circuit breakers are similar, except for the primary voltage rating.

Important differences in the supply system are:

1. The system at location *A* is 34,500 volts, while at location *B* it is 24,000 volts.

2. Location *A* has 350 miles of connected high-voltage line, while location *B* has only approximately four miles. The 350 miles of line supplying location *A* consist of a 34.5-kv loop. A spur line four miles long supplies the furnace, and there is no other load on this line. The 24-kv line supplying location *B* originates at a step-down substation four miles away. There is one other furnace on this same line, approximately 50 feet beyond the one tested, where the line terminates.

3. The 34.5-kv loop supplying location *A* is normally grounded through a Petersen coil at only one point, about 26 miles from the furnace, while the line supplying location *B* is solidly grounded at a substation four miles away.

4. Location *A* is served by a 650-foot length of three-conductor underground cable just ahead of the oil circuit breaker (see Figure 2), while there is no underground cable at all in the circuit at location *B*.

5. The only surge protection on location *A* is a bank of obsolete oxide film arresters located on the source side of the underground cable, approximately 700 feet from the furnace, while location *B* is protected with modern lightning arresters at the end of the line, approximately 50 feet beyond the test location.

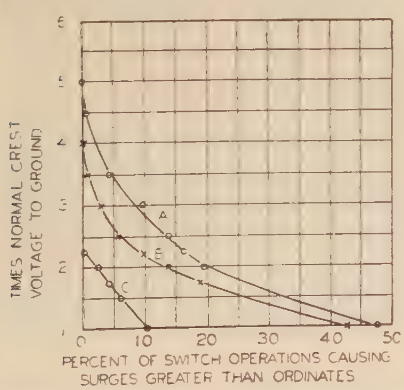


Figure 6. Comparison of Klydonograph tests at location *A*

Curve *A*—Normal condition with system grounded through Petersen coil

Curve *B*—System solidly grounded at three points

Curve *C*—Underground cable on source side of circuit breaker replaced by overhead line

Figure 7. Cathode-ray oscillogram of tripping circuit breaker under load—moving film

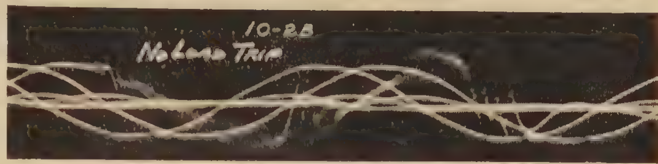


Figure 8. Cathode-ray oscillogram of no-load tripping operation — moving film

At both installations the furnace shell, transformer tank, and electrode control have a common ground.

Test Results

The circuit used in making tests at location *A* is shown in Figure 2. The Klydonograph only was used on this installation.

Some of the data obtained at location *B* were made with the Klydonograph and magnetic oscillograph only. The circuit is shown in Figure 3. Other tests were made at location *B* in which the cathode-ray oscillograph was used on one phase as well as the Klydonograph and magnetic oscillograph. The location of the test apparatus in the circuit is shown in Figure 4. Still other tests were made with a circuit similar to Figure 4 except that the potential transformers on the line side of the circuit breaker were connected across the three poles of the breaker to record breaker voltage on the magnetic oscillograph.

A total of approximately 2400 switch operations was recorded on the Klydonograph, resulting in approximately 850 surge figures of such magnitude as to be measurable. In addition a total of approximately 120 normal heats were recorded on the Klydonograph. At location *B* 110 switch operations were recorded on cathode-ray oscillograms, and 150 switch operations were recorded on magnetic oscillograms.

The results are summarized in comparative form in Table II.

In analyzing the results the Klydonograph data only were used for quantitative results such as presented in the percentage distribution curves of Figures 5 and 6. It was not possible to obtain sufficient oscillographic data for such a statistical analysis. However, in general, the magnitudes recorded on oscillograms gave a reasonable check with those from the Klydonograph. The oscillograms were used chiefly to obtain information on the nature of the surge wave shapes, such as natural frequencies, restriking, and so forth. The only reliable information about wave shape yielded by Klydonograms is polarity, and even this must sometimes be questioned, as explained later.

An attempt was made to plan the test procedure so that answers might be obtained to as many as possible of the following questions:

1. *What is the polarity of the Klydonograph surge figures?* (The character of positive and negative surges is quite different, and the two are easily distinguished.[2]) The results obtained in connection with this question are taken up in detail later.

2. *Are overvoltages caused by the furnace load?* At location *A* a number of regular heats were attended, and the Klydonograph film was moved by hand after each switch operation and after each time that the electrodes were raised or lowered. By this means it was determined that the only overvoltages obtained occurred on switching. No such tests were made at location *B*, but an examination of the Klydonograph records leads to the tentative conclusion that here also surges were caused only by switching.

3. *Are overvoltages caused by raising and lowering the electrodes to interrupt or start the arc?* By the same procedure of moving

Figure 9. Cathode-ray oscillogram of no-load closing operation — moving film

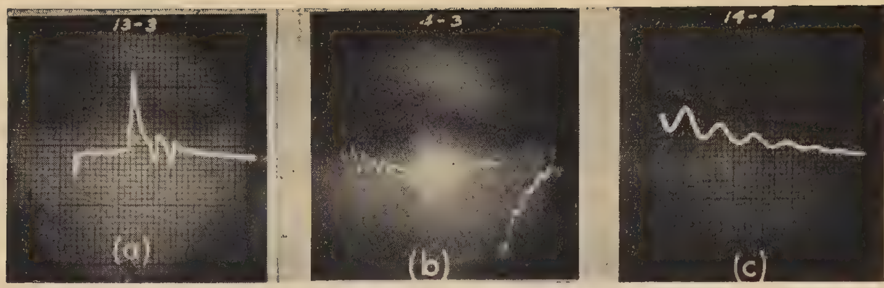


Figure 10. Cathode-ray oscillograms of no-load closing operation using single sweep of the oscillograph

(*a*). 60-cycle sweep
(*b*). 300-cycle sweep
(*c*). 900-cycle sweep

the film as previously described, it was also ascertained at location *A* that lowering and raising the electrodes did not cause voltage surges. (Again no tests were made at location *B*.) This result is not surprising since electrode motion is very slow and interruption probably occurs at normal current zero without any appreciable disturbance.

4. *Are overvoltages caused by closing the breaker on load?* Closing the breaker on load was done at location *B* only. No overvoltages were observed from this operation, although as would be expected, current surges were obtained.

5. *Are overvoltages caused by tripping the breaker underload?* Surges of several times normal were obtained on both installations by tripping underload during the early part of a heat. However as stated previously, it was found that load tripping after meltdown when the arc is short and hot did not produce appreciable overvoltages.

6. *In no-load switching are the severest surges caused by closing or by tripping?* The results are discussed under "No-Load Switching Tests."

7. *What is the effect on no-load switching surges of:*

(*a*). *Transformer tap position?* The tests on both installations showed that the transformer tap position did not have any important effect on the magnitude of switching surges.

(*b*). *Supply-system grounding?* This test was made at location *A* only, and the results are shown in Figure 6 and are discussed in detail later.

(*c*). *The effect of the underground cable at location A?* Results of this test are shown in Figure 6 and are discussed in detail later.

8. *What are the natural frequencies of the switching transients?* The natural frequencies obtained from the cathode-ray oscillograms at location *B* are shown in Table II, and typical oscillograms are shown in Figures 7 to 14, inclusive.

No-Load Switching Tests

Since the voltage surges were found to be caused mostly by switching, and since the usual procedure is to switch no-load, the bulk of the testing was devoted to accumulating data on no-load switching.

An analysis of these data reveals some striking differences between the results obtained at the two different locations, and these differences are believed to be explained by the unusual combination of circuit characteristics at location *A*.

It is believed that location *A* is rather unique and is of particular interest for several reasons.

1. It is one of a very few 34,500-volt furnace-transformer installations in this country.

2. The presence of the underground cable ahead of the circuit breaker, together with the great length of connected high-voltage line and the Petersen-coil ground, is somewhat unusual.

It will be noticed in Table II that the maximum surge obtained on location *A* was considerably greater than location *B*. The difference is even more evident in Figure 5 in which curve *A* represents switching surges at location *A*, and curve *B* represents those at location *B*. The difference between typical Klydonograph films from location *A* and location *B* is striking, because of the much greater size and frequency of surge figures on films obtained at location *A*. Figure 15 is a sample Klydonogram from location *A*. Part of the difference between the severity of surges at locations *A* and *B* may possibly be explained by the better lightning protection at location *B*. (See Table I.) However, in both cases the lightning arresters are on the other side of the circuit breaker from the surge-measuring instruments. It is believed that the lightning arresters at location *B* were not operative to any great extent in reducing the maximum value of the surges because there was no tendency of the percentage distribution curve to flatten off near the upper voltage limit. (See Figure 5.)

Several other differences between the results obtained at locations *A* and *B* are of interest.

Polarity

At location *A* all of the Klydonograph figures for switching were unidirectional in nature and approximately equally distributed between positive and negative. At location *B* nearly all of the figures appeared to be positive. The explanation for this difference is believed to be that surges at location *A* were truly unidirectional, while those at location *B* as evidenced by the supporting oscillographic films, resulted in both positive and negative voltages for each surge in nearly every case. This being the case, the positive figures would tend to cover up completely the negative figures since they are more than twice as large. Also it happens that the 60-cycle normal voltage line which appears in records where the film is clock-driven is sufficiently wide to hide completely all but the very highest voltage negative figures. As it happened at location *A*, a slight unbalance in the line-to-ground voltages caused the 60-cycle normal-voltage line to appear on only one phase, permitting all negative surge figures to be seen easily on the other two phases. Another interesting point is that in most cases at location *A* the surges appeared to be of the same polarity and the same order of magnitude on all three phases indicating that the entire winding rose above ground, while this was not at all the case at location *B*.

Comparison of Closing and Tripping Surges

At location *A* it was surprising to find that nearly all appreciable no-load switching surges were obtained when the breaker was closed, only a very few surges of minor magnitude being obtained for

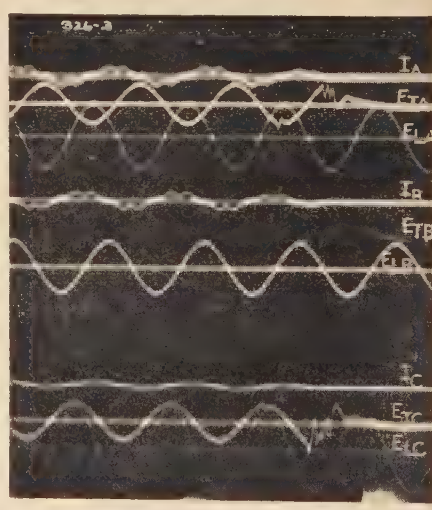


Figure 11. Magnetic oscillogram of no-load tripping recording line to neutral voltages on transformer side and supply side of the breaker—Figure 4

I_A, I_B, I_C—Line currents

E_{TA}, E_{TB}, E_{TC}—Transformer voltage of phases *A*, *B*, and *C*

E_{LA}, E_{LB}, E_{LC}—Voltages on the supply side of the breaker of phases *A*, *B*, and *C*

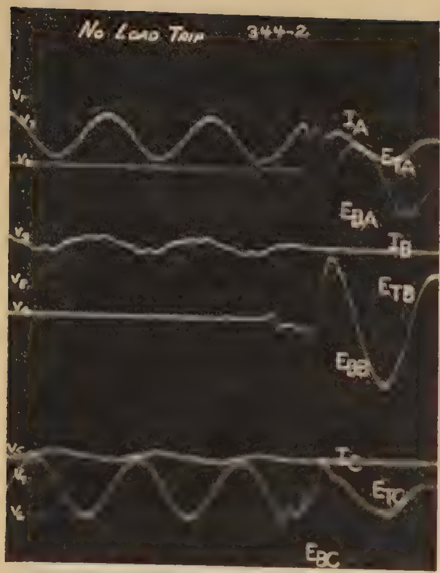


Figure 12. Magnetic oscillogram of no-load tripping operation recording transformer voltages and breaker voltages

I_A, I_B, I_C—Line currents

E_{TA}, E_{TB}, E_{TC}—Transformer voltages of phases A, B, and C

E_{BA}, E_{BB}, E_{BC}—Voltages across breaker contacts in phases A, B, and C

no-load tripping. At location B on the other hand, switching surges were approximately equally distributed between closing and tripping both in number and magnitude, with the balance slightly in favor of tripping.

The closings were distinguished from the trippings by moving the Klydonograph film by hand after each closing or tripping. This resulted in a normal 60-cycle line on the film after each closing and a blank space after each tripping. In Figure 15 all of the important surge figures occur at the start of the 60-cycle line, showing that they were produced by closing the breaker.

Effect of Supply-System Grounding

Tests were made to determine the effect of system grounding at location A, and the results are shown in Figure 6 by a comparison of curves A and B. There seems to be an appreciable reduction in the magnitude of switching surges with the system solidly grounded as represented by curve B. For this test the system was solidly grounded at three points, the nearest point being at the junction of the spur line and 34.5-kv loop four miles away.

Effect of Underground Cable at Location A

Fortunately it was also possible to make tests with the underground cable out of service and replaced by a temporary overhead line. The results are shown in curve C of Figure 6, and the great reduction in the magnitude of switching surges is evident. The maximum surge was reduced from five to approximately two and one half times normal.

It is believed that the great difference between results obtained at locations A and B are due chiefly to three factors, namely:

1. Better surge protection at location B.
2. Presence of the capacitance of the underground cable and great length of connected line at location A.
3. The difference in grounding of the two supply systems.

Of these, based on the result of the tests, it is believed that the presence of the cable is most important.

The great effect of the cable might be explained by the cumulative effect which occurs in capacitive circuits when restriking occurs and which is absent in inductive circuits. The effect of the system grounding may be partly because of more rapid draining of charge from the cable with the solidly grounded condition.

Typical Oscillograms

Figures 7 to 9 are cathode-ray oscillograms made with a moving film, and Figure 10 is a cathode-ray oscillogram in which the single-sweep arrangement on

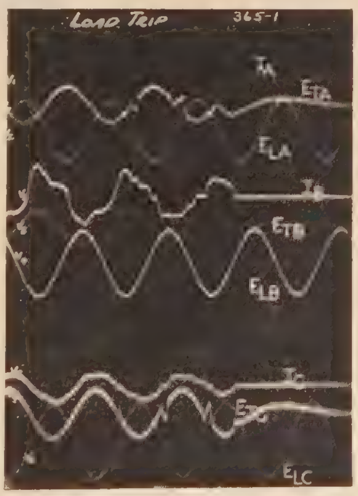


Figure 13. Magnetic oscillogram of tripping underload recording transformer voltages and line to neutral voltages on the supply side of the breaker—Figure 4

I_A, I_B, I_C—Line currents

E_{TA}, E_{TB}, E_{TC}—Transformer voltage of phases A, B, and C

E_{LA}, E_{LB}, E_{LC}—Voltages on the supply side of the breaker of phases A, B, and C

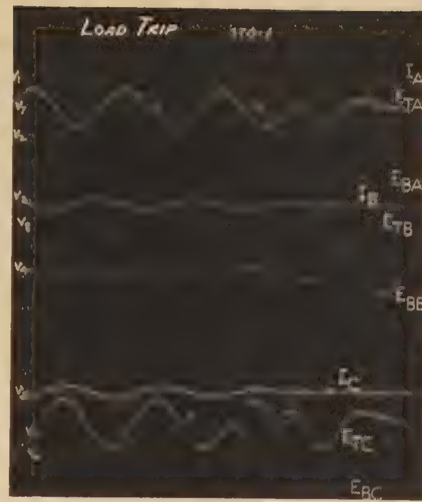


Figure 14. Magnetic oscillogram of tripping under load recording transformer voltages and breaker voltages

I_A, I_B, I_C—Line currents

E_{TA}, E_{TB}, E_{TC}—Transformer voltages of phases A, B, and C

E_{BA}, E_{BB}, E_{BC}—Voltages across breaker contacts in phases A, B, and C

the oscillograph was used. Figures 11 to 14 are magnetic oscillograms.

Figure 7 is a record of a load tripping operation in which restriking occurred twice, and the final interruption resulted in an inductive kick to approximately twice normal.

Figure 8 is a typical no-load tripping record. It will be observed that arcing lasted for approximately one cycle, and this is typical as evidenced also by the magnetic oscillograms. The large lower frequency oscillations are restriking at successive current zeros. It was not possible to resolve the very high-frequency oscillations between these points on any of the film records obtained.

Figure 9 is a typical no-load closing operation in which the shape of the long-time transient is attributed to the fact that the circuit was made at different times on two phases.

Figure 10*a* is a no-load closing in which a 300-cycle sweep was used. A similar closing is shown in 10*b* at a 60-cycle sweep, and at 10*c* in a 900-cycle sweep. The frequency is approximately 6,500 cycles. In 10*b* the effect of making at different instants of time on different poles of the breaker is clearly shown. The maximum voltage obtained, however, is only approximately normal crest voltage.

Natural frequency oscillations obtained on no-load tripping are clearly seen in Figures 11 and 12, and the absence of these oscillations is striking in the load trip magnetic oscillograms in Figures 13

and 14. However, it will be observed that in the load trip records, arcing is extended over nearly two cycles while the arcing period is approximately one cycle in the case of no-load tripping.

Most of the film records obtained for both tripping and closing show three or more restrikes and making or breaking of the circuit at different times on the different phases.

Conclusions

1. Voltage surges occurring on the high-voltage terminals of arc-furnace transformers are caused largely by switching.

2. Tripping out under load can cause high overvoltages chiefly in the early parts of the heat when the arc space is long and cools rapidly. Load tripping, during the later parts of the heat may result in lower voltage transients than no-load tripping but in extended arcing. As a general practice it is better to raise the electrodes before tripping the breaker.

3. Since no-load switching is general practice, the problem of switching surges on furnace circuits is not essentially different from that of other power circuits, except that switching is more frequent and the circuit constants may be quite different.

4. Because the problem of overvoltages, at least on the high-voltage side, reduces to one of switching surges, the characteristics of the supply system, as well as of the furnace circuit and the switch, will determine the magnitude of voltage surges obtained. In particular the grounding of the power system is important.

5. The presence of a length of underground cable on the source side of the circuit breaker used for switching may be the cause of excessive overvoltages.

6. It is realized that the data presented here are far from complete or conclusive, and it is believed that further study of the problem of voltage transients in arc furnaces is warranted, particularly with a view to obtaining measurements on the low-voltage side of the transformer.

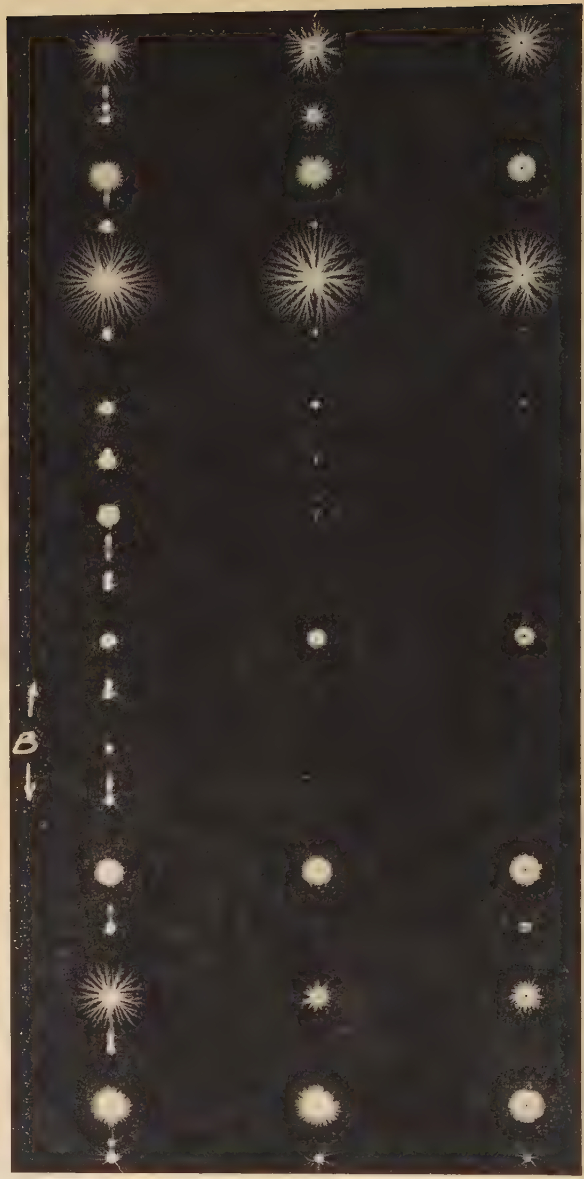


Figure 15. Typical section of Klydonograph film obtained in no-load switching tests at location *A*

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1943 Supplement to Electrical Engineering—Transactions Section

Aircraft Contactors

F. J. RUSSELL
MEMBER AIEE

A. P. CHARBONNEAU
NONMEMBER AIEE

THE growing complexity of the electrical systems in use on modern aircraft has given rise to need for a complete line of magnetically operated switches or relays suited to conditions encountered in this type of service. Relays may be classified according to use as control relays and power relays, or in the terminology of the industrial-control engineer, relays and contactors. This paper will be confined to a discussion of power relays or contactors. Most aircraft in use at the present time have 24-volt d-c systems; consequently only low-voltage d-c contactors will be considered.

Commercial contactors of the required ratings are larger and heavier than practical for aircraft partially because

1. Conditions of use do not limit these factors so rigidly.
2. The longer life expected from them.
3. The greater insulation distances required for higher voltages.

A background of experience obtained from commercial contactors can contribute greatly to the design of aircraft switches, but numerous special conditions must also be considered. Changes in barometric pressure from 30 inches of mercury at sea level to about 5.54 inches at 40,000 feet, and temperature ranges from −50 degrees Fahrenheit to +200 degrees Fahrenheit are encountered. Vibration and shock may be met, and the switches must be designed to withstand them. Vibration tests consisting of simple harmonic motion with an amplitude of 0.03 inch (0.06 inch total displacement) and a frequency varied from 10 to 55 cycles per second have been made part of the specifications. Switches must operate in any position. Coil temperature rise must be limited to 70 degrees centigrade above a 25 degrees centigrade ambient.

Some aircraft accessories using contactors are listed in Table I. Loads are, therefore, resistance, inductance, lamps, and motors. Contactors are used on loads as low as 25 amperes and may carry 200 amperes or more. Circuit requirements may be for single-pole single-throw; single-pole double-throw; double-pole single-throw; and double-pole double-throw contactors. When used for the control of reversing motors double-throw contactors have two coils each operating a set of contacts controlling direction and mechanically interlocked to prevent simultaneous operation.

Design of a line of contactors naturally divides itself into consideration of several phases:

1. Contacts.
2. Magnet.
3. Arrangement, mounting, and special features.

These phases will be considered separately and in the order listed.

Contacts

The heart of a contactor is its contacts. The contact material must be one which does not collect a film which offers high resistance to the passage of current either when the contacts are open, while closed and heated by the current flow, or under arcing. They must therefore be made of a material which does not form nonconducting oxides and which, because of the low voltage, has a low resistance. Silver meets these requirements, but, because of transfer and welding problems, additions to the silver, as for example cadmium oxide without alloying, have been found to give superior properties. Various other additions have been proposed including cadmium sulphide, lead sulphide, tungsten, and molybdenum. The contacts must be securely fastened to the contact plates to obtain a permanent low resistance bond. Brazing is a satisfactory method. A superior product may be obtained, however, by a process in which the constituents are granulated or powdered, mixed, and molded under high pressure direct to the backing material. A grid of serrations on the backing plate interlocks with the molded material to form a secure anchor and a permanently low resistance joint. The density of the contact material may be controlled by the pressure applied in molding them. Contacts formed by this process have several important advantages.

1. A low resistance contact having the properties of silver.
2. A contact having in it other metallic materials to obtain less transfer and to reduce welding.
3. A contact with a controlled density affecting its bounce characteristics and permitting welds to be broken.
4. A contact extremely uniform and easy to manufacture.

Contacts opening and closing d-c circuits are subject to several types of failure. Transfer of material from one contact to the other in the electric arc is a well-known phenomena which has caused considerable trouble to the designers of d-c switches using contacts containing silver. Transfer occurs at any time an arc exists between the contact faces and is a function of the current in the arc. It is apparent therefore that transfer may occur during the time that the contacts are arcing when they close and also during the time the contacts are opening before the arc is extinguished. It is important that arcing time be reduced to a minimum to limit the amount of transfer. Numerous readings taken with an instrument designed for measuring arcing time[1] show that the arcing time on closure is influenced by the initial contact pressure. Figure 1*A* is a curve showing the rela-

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F. J. Russell and A. P. Charbonneau are both with Cutler-Hammer, Incorporated, Milwaukee, Wis. Mr. Russell is supervising engineer.

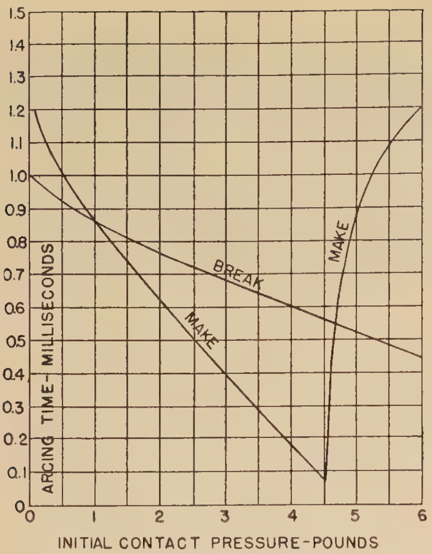

Figure 1A. Curves showing arcing time for various contact pressures

tion of arcing time to initial contact pressure for a typical contactor and shows that as the contact pressure is increased the arcing time on closure is reduced until a critical value is reached. Below this value the bounce occurs when the contact plate itself rebounds while above this value the arcing time increases because the entire moving assembly of contact and armature bounces. The weight of the moving contact plate and the armature must therefore be correlated with the initial contact pressure to obtain the most satisfactory values. Figure 1*B* shows the variation in arcing time of a contactor as the coil voltage is changed. Here again a point is reached where the entire armature and contact assembly bounces as the voltage is decreased.

The design should be such as to prevent "armature bounce" from occurring in the range of voltages over which the contactor is to operate. Reference to the curve shows that the maximum contact

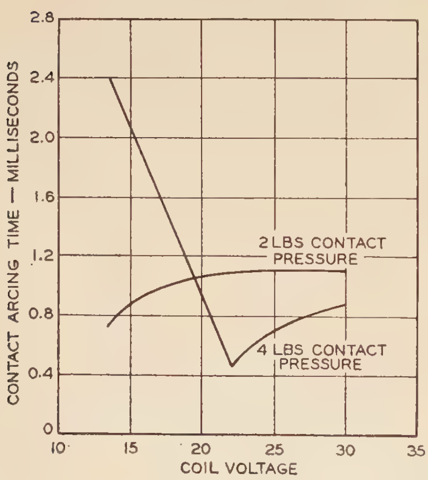

Figure 1B. Curves showing arcing time for various coil voltages

pressure is limited by satisfactory operation on low voltage (18-volts hot coil). A minimum value which will keep the arcing time on the lower part of the curve should be used to keep transfer to a minimum.

The arc formed when contacts bounce on closing may, if it is of sufficient duration or intensity, fuse the contact material and cause rapid wear. For contacts of the character stated, the molten metal is flattened and in some cases splashed when the contacts reclose. On 30 volts this begins to occur with currents of about 100 amperes. The high points are, under these conditions, leveled off so that the effects of transfer are nullified. Contacts operated above 100 amperes remain flat and have a fine granulated or mottled appearance.

Figure 3 shows typical contacts before and after being used. A typical case of transfer may be seen in Figure 3*c* while a contact which has remained level is shown in Figure 3*d*.

If the current is sufficiently high the degree of fusing may eventually cause welding. Factors which determine the current at which welding takes place include

1. The materials from which the contacts are made.
2. The thermal capacity of the contacts.
3. The degree of flattening of projections thrown up on the contact faces (controlled by the initial contact pressure).

Cadmium oxide mixed into the contacts retards the tendency toward welding and transfer and is therefore beneficial. Contacts of high thermal capacity and conductivity take the heat away from the point of arcing and reduce the amount of molten material formed. The blow which tends to flatten projections is dependent on the initial contact pressure and the velocity of closing, and it is therefore desirable to keep these values as high as possible. On opening, if the density of the contacts is low, the welds will be broken when the contacts part, particularly if a blow or shock is imparted to them. The force necessary to break the weld is determined to some degree by the materials used for the contacts and the form and size of the particles used in making them.

Contacts should have sufficient wear allowance to compensate for the wear obtained during their life and to provide for the unavoidable variations in manufacturing. The actual wear during the life required for aircraft contactors indicates that a wear allowance of 0.03 inch is sufficient to provide ample safety factor. They should have an initial contact pressure high enough to prevent welding on the highest currents which may have to be made and the pull of the magnet designed to permit this pressure to be used without obtaining armature bounce.

The arc gap required is determined by the voltage for which the contactor is to be used. A value of 0.055 inch has been selected as conservative for 24-volt service.

Magnet

There are several forms in which the operating magnet for contactors may be built. Each has characteristics which determine its suitability for a particular use. All have many common properties and a general analysis of their operation

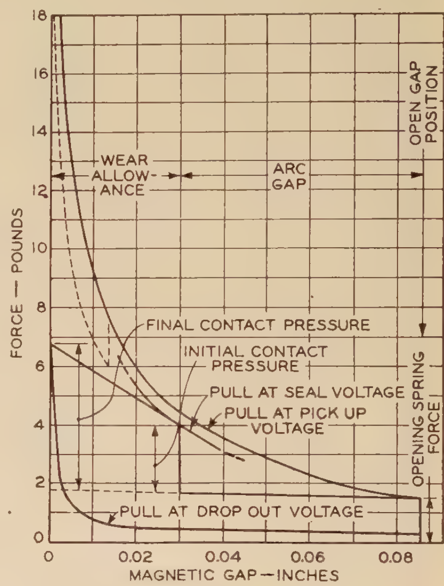

Figure 2. Contactor force analysis

may be made. Figure 2 shows a force diagram for a typical contactor. The curved voltage lines represent the pull of the magnet at various plunger positions. The forces exerted by the springs are shown by the straight lines. For convenience both the magnet-pull curves and the spring forces are plotted as if they were scalar quantities of the same direction. It should be remembered, however, that their direction of application is such as to oppose each other. At the open-gap position the plunger is kept from moving into its sealed position by the balancing or opening spring. The force required is determined by the weight of the moving parts and the acceleration forces to which the contactor may be subjected. A minimum value of $F=(A/G)\times W$ where A is the maximum acceleration to be encountered, G is the acceleration of gravity, and W is the weight of the moving parts. For balanced

structures in which accelerative forces are cancelled the force is determined by the energy required to open the contacts with sufficient velocity to insure breaking the arc quickly. As the plunger is moved toward its sealed position the opening spring is compressed so that this force increases. It is desirable that the rate of increase in this force be kept as low as possible, for an increase in its value increases the magnet pull required for a given initial contact pressure. The contacts are arranged so that they close before the sealed position of the armature is reached. The remaining portion of the stroke is wear allowance. The moving contact are fastened to the armature by means of a spring in such a manner that a force is required to separate them from it. This force is the initial contact pressure and the performance of the contacts depends considerably on its value. Reference to the force diagram shows that the upper limit of the initial contact pressure fixes the shape of the magnet-pull curve, for it is necessary if the contactor is to close smoothly that the curve passing through the initial contact pressure point be a lower voltage than the pickup voltage which is the voltage at which the magnet pull is equal to the opening spring force at the open-gap position. The shape of these curves may be altered to some extent by the shape of the pole faces. The load placed on the power system when the contacts close may be high enough to cause an appreciable drop in system voltage. For instance, the current inrush on some motors used in military aircraft has been found to be as high as 2,000 amperes. The resulting drop in voltage at the contactor coil terminals may reduce the pull to an extent that the contact will not seal completely, resulting in additional arcing of the contacts which may cause them to weld. Voltages as low as eight volts have been obtained on 24-volt systems when heavy currents were drawn. There is the further possibility, if the voltage falls low enough, that the armature may drop back even though the armature reached its sealed position. In this case the voltage drop in the arc will limit the current until the motor accelerates and the system voltage builds up enough to reclose the contactor. Operation under these conditions will burn the contacts severely and will probably result in welding. Dropout occurs when the coil voltage is decreased until the sealed pull is equal to the force from the springs tending to open the contacts. The magnet pull should decrease as the magnetic gap increases in a way so that it is less at the point where the contacts just touch than the force exerted by the opening spring to avoid hesitancy in dropping out. In other words, the curve passing through the point of zero wear allowance must be of a higher voltage than that of the drop-out curve.

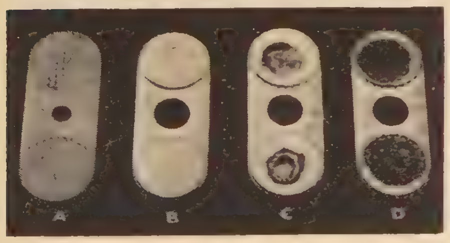

Figure 3. Contact plates

A. Backing plate showing serrations
B. Finished contact plate
C. Contact plate showing transfer
D. Contact plate after 50,000 operations at rated current

The coil must have the required ampere turns to produce the necessary pull at the lowest voltage on which it is to operate. The minimum open-circuit voltage on 24-volt systems has been set at 18 volts, and therefore the pickup voltage must not exceed this value when the coil has attained its ultimate temperature. Since the temperature rise of the coil is limited to 70 degrees centigrade above a 25 degrees centigrade ambient, this final temperature is 95 degrees centigrade or the contactor must pick up on 14.2 volts when the coil is at 25 degrees centigrade if it is to operate on 18 volts with a hot coil.

Table I. Aircraft Accessories Using Contactors

	Load
Battery disconnect	General
Engine starter	Motors
Engine synchronizer	Motors
Propeller pitch control	Motors
Wheel retraction	Motors
Flap control	Motors
Lighting	Lamps
Fuel transfer	Motors
Heating	Heaters
Gun firing	Solenoids
Bomb release	Solenoids
Turret operation	Motors

The maximum voltage encountered on 24-volt systems is 28.5 volts. The coil must not exceed its allowed temperature rise when this voltage is applied. These factors fix the resistance of the coil and its radiating area.

Contractors which are not energized for sufficiently long periods to attain their ultimate temperature may have coils designed for part-time duty only. Advantage may be taken of this fact to reduce the size of coils used on intermittent-duty contactors or greater pull may be obtained from coils the same size as continuous-duty coils where special conditions must be met.

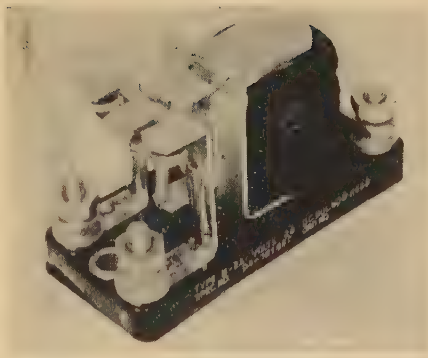

Figure 4. 25-ampere balanced-armature aircraft contactor

An intermittent-duty coil may be protected by the use of a resistor to make it suitable for continuous duty. The resistor is inserted in series with the coil after the main contacts have been operated. A set of auxiliary contacts are used to insert the resistor, which may be additional turns on the coil itself. The auxiliary contacts must be arranged to open after the main contacts close and the resistance value and the shape of the pole faces should be selected to keep the magnet pull greater at all points than the spring force to avoid chattering or telegraphing. See the dotted curve in Figure 3. While this type of construction permits the use of a smaller and lighter coil, the addition of the auxiliary contacts and the additional leads on the coil may completely offset the advantages of this construction. Greater difficulties in adjustment because of the accuracy required on the small switch are to be expected.

The pivoted armature type of magnet consisting of essentially a *U* having the coil mounted on one leg and using the other leg as a return magnetic path and also serving as a support for the armature pivot is a well-known and widely accepted form used a great deal in industrial contactors. The armature lever may be extended in both directions from its pivot and balanced, thereby making it more or less free from shock and the effects of vibration or acceleration. The contacts may be placed at either end of the lever, depending on whether they are to be closed or open when the magnet is energized. Coils are readily replaceable with this type of magnet. The travel of the armature lever in the closed direction is limited by the sealing of the armature face against the core face. In order to insure a reliable contact throughout the life of the contactor and to compensate

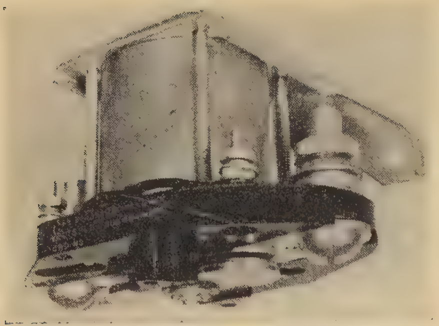

Figure 5. 200-ampere solenoid-type aircraft contactor

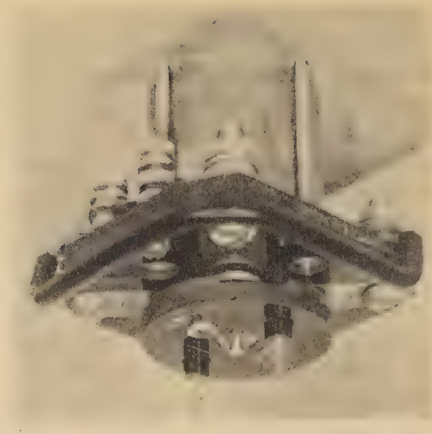

Figure 6. 100-ampere two-pole single-throw aircraft contactor

Figure 7. 50-ampere single-pole double-throw aircraft contactor

for wear of the contacts the contact faces are made to touch before the armature seals, the additional travel being taken up by providing for relative movement between the contacts and the armature lever, either using a spring for this purpose or depending on the flexing of the parts themselves. The latter method has the disadvantage, however, of requiring periodic adjustment of the contact position to compensate for contact wear, thereby causing a very rapid decrease in contact pressure unless the parts are made sufficiently flexible to give the same action as a separate spring. This is a construction frequently used in control relays where the amount of power handled and the wear is small. The pivoted-armature-lever construction can be made to cause a rolling or sliding action between the faces of the contacts which is desirable if the contact material is such as to collect a nonconductive film but which has severe disadvantages for some types of material.

Figure 4 is an illustration of a balanced-armature type of contactor rated at 25 amperes.

The ironclad solenoid type of magnet may also be used. The armature in this case is a plunger moving in the center of the coil and sealing against a stationary core member. The coil is enclosed in a shell which forms the return magnetic path and also serves as protection. The stationary contacts are mounted at one end of the coil and the moving contacts travel in a straight line to engage them. For the type of contacts being used this is an ideal arrangement because sliding or rolling on contacts of the character under discussion is not desirable. The face of the plunger in this case is conical in form to produce the desired pull curve.

A contactor of the solenoid type rated at 200 amperes is shown in Figure 5.

Arrangement Mounting and Special Features

In the design of the line of solenoid operated switches the coil is enclosed by the shell for protection and the assembly fastened together to make it a readily replaceable unit. The mounting bracket forms part of the magnetic path to conserve material and to reduce the weight. Since it is in intimate thermal contact with the coil shell and the central core, the radiation from the bracket aids in the dissipation of the heat produced in the coil. The stationary contacts are mounted on a bakelite terminal board placed on the opposite end of the coil from the mounting bracket. Iron pole pieces under the contacts serve to concentrate the magnetic field across the contact faces to obtain a blowout action. The plunger is arranged to impart a blow to the contact when the coil is de-energized by having the force from both the contact spring and the opening spring applied to the plunger in the closed position. When the coil is de-energized, the plunger moves a distance equal to the wear allowance before striking the contact plate, thereby tending to break any weld that should be found.

Double-pole single-throw contactors are similar to the single-pole contactors, except that a second set of contacts is provided. Figure 6 shows the 100-ampere two-pole contactor.

The reversing contactor shown in Figure 7 consists of two single-pole or double-pole contactors mounted on a common bracket and having a common terminal board. An interlocking bar to prevent simultaneous operation permits either contactor to be operated independently.

Conclusion

Contactors designed particularly for use on aircraft may be smaller and lighter than those having comparable current ratings used commercially. In order to obtain the most satisfactory design careful consideration must be given to the basic requirements and to the conditions of operation. Analysis of the functions of the component parts makes it possible to design them to obtain contactors having maximum flexibility in application, minimum maintenance for long and dependable operation, and smallest size.

Reference

1. An Instrument for the Determination of Contact Making and Breaking Time, **Walther Richter, William H. Elliot.** AIEE Transactions, volume 62, 1943, January section, pages 14–16.

Interim Report on Application and Operation of Out-of-Step Protection

AIEE RELAY SUBCOMMITTEE

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports in this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the Standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment without changing the fundamental basis on which the present standards have been prepared.

Synopsis: Out-of-step protection of synchronous machines and system interconnections is now demanding serious consideration, first because the heavy loading of machines and transmission lines has introduced many stability problems and secondly, because outages resulting from out-of-step conditions may interfere seriously with the war effort. The relay subcommittee has, therefore, considered it timely to prepare a report on the subject and has appointed a working group for that purpose.

The report describes briefly out-of-step phenomena, the methods of protection that are available, and a cross section of the practices and requirements of representative utility systems.

Out-of-Step Phenomena

It is well recognized that when a synchronous machine pulls out-of-step with another synchronous machine or group of machines, a violent disturbance is set up which may damage the machines and cause undesirable operation of relays used to protect against overload or short circuit. Detailed explanations of the actual phenomena have been published.[5–9] It is believed, however, that a brief review of the behavior of the current and voltage during an out-of-step condition may enlighten the discussion on the application of devices and methods for protecting machines, for blocking undesirable tripping of fault protective relays, and for opening interconnections at selected points during out-of-step conditions.

A synchronous machine can be represented by an equivalent reactance and an internal voltage. For steady-state conditions, this equivalent reactance is equal to the synchronous reactance modified in accordance with the machine excitation. For sudden changes in armature current, such as those that occur during an out-of-step condition, the equivalent reactance is generally more nearly represented by the transient reactance of the machine.

When two synchronous machines are paralleled, the current interchange between them is determined by the vector difference of their internal voltages applied to the interconnecting impedance, which includes the reactance of the machines as well as any impedance between machines. Neglecting charging current and any tapped-off load current, the current is the same at all points in the interconnection and lags the difference voltage by the impedance angle of the interconnection. The voltage at any point is equal to the internal voltage of the leading machine minus the impedance drop to the point. See Figure 1. With this basic understanding it is possible to determine either analytically or graphically the magnitudes and relative phase angles of the current and voltage at any point in an interconnection for any magnitude and angular separation of internal voltages.

When the machines pull out of step, the internal voltages swing apart through an angle of 360 electrical degrees for each complete slip cycle. As this angle of separation progresses, it causes a difference voltage which starts at a minimum for the inphase position, builds up to a maximum at 180 degrees out-of-phase, and decreases again to a minimum at 360 degrees. Therefore, there will be pulsations in current and voltage at every point in the interconnection. The current will pulsate in magnitude similarly to the difference voltage; that is, it will be a minimum for the inphase position and a maximum at 180 degrees out-of-phase. The voltage at any point will also pulsate in magnitude, but it will be a maximum for the inphase position and a minimum at or near 180 degrees out-of-phase. The minimum value will vary throughout the interconnection, reaching zero at one point which, for equal internal voltages, would be the electrical center of the interconnection. If the internal voltages are unequal, the point of zero minimum voltage will be proportionally nearer the end of lowest internal voltage.

The curves of Figure 1 show how the interchange current, the voltage, and the angle between the two vary during a complete slip cycle at two points in a typical interconnection. Equal internal voltages and no intermediate load are assumed. The points shown are the high-voltage busses at each end of an interconnection consisting of a synchronous machine and transformer at each end with a 60-degree impedance line between. From these curves, it is possible to predict the performance of voltage-, current-, watt-, or distance-type relay elements located at each end of the line, taking into account the relay connections and the period of a slip cycle.

Protection of Individual Synchronous Machines

Individual synchronous machines can be protected against out-of-step operation by devices that recognize a condition which may cause pull-out unless corrected or by devices that detect an actual out-of-step condition. The more complete protection would include devices of both types. In general, the degree of protection warranted depends on whether the station is automatic or attended, whether the machines are generators or motors, and upon the importance of the machine.

Before Loss of Synchronism

A synchronous machine may pull out of step because of any one or a combination of the following conditions:

1. Load in excess of pull-out with normal excitation.

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Personnel of the working group on out-of-step protection of the relay subcommittee: H. R. Vaughan, *sponsor;* C. R. Mason, G. Steeb, J. H. Vivian, E. E. George, E. W. Knapp, J. T. Logan, J. J. Samson, E. C. Sawyer.

This interim report was prepared by the working group on out-of-step protection of the relay subcommittee of the AIEE committee on protective devices for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been approved formally by the Standards committee nor the board of directors.

Acknowledgment is given to L. N. Crichton (deceased) and to C. R. Mason for the preliminary report that each prepared while he was serving as sponsor of the group.

Table I. Summary of Replies to Questionnaire

Operating Company	1	2	3	4	5	6	7	8	9	10	Comments
	Question Number from Questionnaire										
A	Yes	Field failure and reverse current relays on field feeders—record good	Trip	None. Operators instructed to trip if out-of-step exists for extended time	Alarm	Yes	Yes	Time delay on carrier tripping	c	Incorporated as part of relay scheme	Stability problem has been reduced by carrier relaying, set to give about 15 cycles clearing time. Carrier relaying has adjustable time delay to allow electrical center of out-of-step condition to shift through line section. Some difficulty in trying to set for selective tripping
B	Yes (Frequency changers)	Overload relays used to disconnect frequency changers in case of pull-out	Trip	None	Trip	No	No	None	a	Keep relaying schemes separate	Steam stations rigidly interconnected
C	Yes	Detection of condition left to operator	Trip	Operator must identify condition	Trip	No	Yes (on swing)	None	a	———	Closely coupled system. No cases of actual instability in 15 years. Severe load swings with interconnected system have caused line relays to trip
D	Yes	None	Alarm	None	Alarm	Yes	Yes	Carrier relaying with out-of-step blocking	c	Separate	Blocking relays available with carrier relays but not necessary where located at present time
E	Yes	Some generators equipped with overload relays with long time setting	Alarm	Same as (2)	Alarm	Yes	Yes	Carrier relaying with blocking	b	———	In one case of loss-of-field, overload relays tripped generator. In another case, generator slipped poles for a minute or two without relays tripping and pulled back in step when excitation was gradually restored
F	Yes	None	Trip	None	Trip	Yes	Yes	None now. Carrier relays with blocking being installed	c	No experience	Hope to arrange out-of-step blocking so that system will separate at point where it can be conveniently resynchronized
G	Yes	None at present	Alarm	None at present	Trip	Yes	No	None	c	Separate	Loss of or underexcitation considered serious subject. Different machines behave differently. Protection should probably be made to fit characteristics of machine
H	Yes	Generators none. Synchronous condensers have overload protection	Trip	None	Trip	Yes	Yes	None	c	Incorporate in line relays if possible	In some instances might be desirable to block tripping for one or more slip cycles and then permit tripping if synchronism is not regained. Number of slip cycles of blocking should be adjustable. Relays should be blocked from tripping caused by power swings on electrically long lines
I	Yes	None	Trip	None	Trip	Yes	Yes	None at present		Separate	Numerous cases of pull-out between steam plant and small hydroelectric plant and with neighboring system interconnected through long tie. Relays that normally operate during out-of-step split system at neutral load point without serious inconvenience
J	Yes	None on generators. Overcurrent protection on large synchronous motors	Alarm	None except overcurrent relays on large sychronous motors	Alarm	Yes	Yes	At tie points between load areas, line relays are induction type with long time reset to act as notching relays during out-of-step	c	Separate if possible and practicable	Line relays not blocked during out-of-step, but settings are graded to favor tripping at preselected points. System being essentially metropolitan with high load densities is relatively stable
K	Yes	None. Operators instructed to recognize condition	Trip	None	Trip	Yes	Yes	None	c	Incorporate in line relays where possible	Most cases of machine pull-out caused by underexcitation. Line relays always operate at one or more locations in case of system instability. Neglecting economics of installation, would prefer to block all line relays subject to out-of-step tripping except at selected locations where tripping causes the least loss of load and permits quickest restoration of ties
L	Yes	One machine has instantaneous overcurrent relay set above load current to cut out portion of field resistance	Alarm	None	Trip	Yes	Yes	None	c	No opinion	Recent extended interconnections expected to add to out-of-step problems
M	No (Large generators)	None except to trip large generators off line in case of loss of excitation	Trip	None	Trip	Yes	Yes	Blocking and selective tripping scheme which operates with line impedance relays without carrier current satisfactory	c	In some cases desirable to have blocking devices separate. Most desirable line-relay settings may not be best for blocking devices	Operating record for out-of-step blocking scheme for three years. 13 correct desirable, 6 undesirable but correct for the conditions existing, 3 incorrect

N	Yes	None. Unusual operation of voltage regulator gives audible signal that trouble exists	Trip	None except voltage regulator as in (2)	Trip	One case	Yes	None	a	Incorporate in line relays if it does not unduly complicate the protective relays	Operators are educated by discussions, illustrated by moving pictures of meter action during actual out-of-step, to recognize machine causing trouble. This system is relatively compact so that instability is not probable if faults are cleared reasonably fast
O	Yes	On small semiautomatic station use current relay in series with field of alternator to shut down machine after time delay in case field current drops to zero. Not used on major generators	Alarm	None	Trip	Yes	Yes	None	b	Incorporate in line relays	A reliable scheme of positive detection that a particular machine is out-of-step would be desirable
P	None lately	Transient load anticipator on one machine only	Alarm	None	Alarm	Yes	No	Blocking in conjunction with carrier where carrier relaying is used. Record is good	c	No experience except in conjunction with carrier	System is operating near stability limit because of heavily loaded generators at high power factor and highly fluctuating load

2. A sudden change of large magnitude in load.

3. Drop in applied voltage (motor).

4. Abnormally low or complete loss of excitation.

Complete protection against probable loss of synchronism involves devices that will detect any one of the aforementioned conditions.

Load in Excess of Pull-Out With Normal Excitation

In general, the steady-state pull-out torque of a synchronous machine with normal excitation involves currents in excess of the thermal capacity of the machine; so devices provided for overload protection will also protect against steady-state pull-out caused by overload. Such devices are usually thermal- or overcurrent-type relays set either to shut the machine down or to sound an alarm. Where the overload device does not provide protection against steady-state pull-out with minimum operating excitation, such protection may be provided by a kilowatt relay set below the minimum pull-out load or by a load limiting device on the prime mover.

Sudden Change in Load

Sudden load changes on generators are usually caused by system faults, loss of large blocks of power, or by tripping off of other generating equipment. When the load change exceeds the transient stability of the machine, the machine will usually pull out of step so quickly that an overload device for detecting probable loss of synchronism is of little value. Automatic voltage regulators and exciters with high speed of response are used as corrective devices. Also in operation are load anticipators for steam turbine generators, which respond to sudden changes in load but which are not affected by gradual changes. The normal function of this device is to initiate a change in steam flow to the prime mover corresponding to a sudden change in load without interfering with the normal functioning of the governor for gradual changes in load.

An inertia relay[1] which responds to rate of frequency change has been developed primarily to expedite immediate circuit reclosure. Two out-of-step protective schemes are being installed which make use of this relay to detect approaching instability through the rate in frequency change as reflected in voltage phase shift. A watt relay is employed to block the inertia relay for system swings resulting from high-speed clearing of faults which do not cause loss of synchronism.

Drop in Applied Voltage

The pull-out torque of a synchronous motor varies almost directly as the applied voltage. A substantial drop in voltage of more than a few cycles duration is therefore quite likely to cause loss in synchronism. Synchronous motors are usually provided with undervoltage protection to prevent continued operation at reduced voltage or reapplication of improper voltage after shutdown. However, this undervoltage protection should have time delay to prevent unnecessary trip-outs on momentary voltage dips and therefore should not be depended on to anticipate an out-of-step condition.

Loss of Excitation

The field excitation of a synchronous machine has an important bearing on its pull-out torque. Figure 2 shows how the steady-state pull-out between a typical turbine generator and power system varies with excitation. Protection against field failure, or inadequate excitation for a given generator output, is desirable from the standpoint of pull-out if it does not otherwise detract from operating reliability.

The American Standards Association Standards for Automatic Stations recommends loss-of-field protection for all types of machines having fields. This is usually provided by an undercurrent relay in the field circuit to trip and lock out a machine that has lost its excitation. If the station is attended, an alarm may be sounded instead. The undercurrent relay should have sufficient time delay to ride through transient conditions of low field current immediately following external short circuits. Either the drop-out current of the relay must be low enough to allow generation at minimum excitation requirements, or its contact circuit must be disconnected during this operating condition. The relay may also be interlocked with the generator or line breaker so that it is operative only when the machine is on the line.

The main function of an undercurrent relay in the field circuit is to detect loss of field because of open circuits in the field or a failure in some part of the excitation system. It does not provide positive protection against inadequate excitation for a given generator output. Furthermore, it would not always be fast enough, if time delay is intentionally provided, to disconnect a machine with inadequate excitation before synchronism is lost.

Some companies have rigid operating instructions regarding the minimum excitation current permitted for a given

generator output. This quantity has generally been under the manual supervision of an operator, but it could be supervised automatically by balancing the kilowatt load against the d-c field strength. Automatic voltage regulators can be compensated to assure adequate excitation, or for generators they could be interlocked with the prime mover throttle so that the generator could not operate with a low field at full throttle. They could also be provided with a device to limit regulator action to a predetermined minimum field current and to indicate when this excitation existed.

When a generator loses field, it draws excitation from the system. Reactive kilovolt-ampere flow into the generator can therefore be used as an indication of inadequate excitation. A more positive scheme is to use reactive current, since the reactive kilovolt-amperes may be held down because of a severe drop in voltage. There are several installations[2] of this type. Reactive current is measured by means of a polyphase reactive kilovolt-ampere relay, with a voltage regulator to maintain a constant voltage on the relay. The reactive-current relay is interlocked with an undervoltage relay in the bus to prevent tripping unless the bus voltage is low enough to make operation of the generator unsafe. The combination is sensitive to the effect of a serious reduction in excitation, regardless of how it is caused, and removes a machine from service quickly enough to minimize the effects of field failure on the rest of the system.

After Loss of Synchronism

A relay operating on current alone, voltage alone, or directional current alone cannot be depended upon to discriminate between an out-of-step condition and a system disturbance. An overcurrent relay with pick-up above maximum load current and time delay greater than maximum fault clearing time may not close its contacts during an out-of-step condition, particularly if the excitation of the machine is low. If the relay pick-up and time delay are set low enough to operate for an out-of-step condition, the relay is quite likely to operate during the period of hunting following a system disturbance. A directional overcurrent relay would have the same weakness. Likewise, a voltage relay with its drop-out set low enough to ride through a fault condition might not operate during out-of-step unless the machine impedance were high so that the terminal voltage would dip to a low value.

A more reliable scheme of protection makes use of a notching element actuated by out-of-step impulses. The notching relay trips the machine or sounds an alarm after a given number of pole slippages, usually three to five pairs. The notching element may be either a mechanical device or a counting chain of auxiliary relays arranged so that each link will notch up in succession as successive out-of-step impulses are given to it.

The notching prerequisite makes it possible to use more sensitive impulse-detecting relays without the possibility of tripping at the time of a line fault. A single impulse will be given to the notching element at the time of a fault. It is therefore necessary to reset the device after each system short circuit. Otherwise, repeated short circuits would eventually cause tripping. This resetting operation can best be performed automatically by means of a timing relay which is set in motion by the first notch and arranged to reset the notching element if the notching sequence is not completed within a specified time.

The total number of notches in the sequence should be as small as possible to minimize stress on the machine and disturbance to the system. However, it must be large enough to insure against operation from other system disturbances, as for example the impulses caused by the successive reclosing of a line breaker. A notching sequence constituting a pole slippage of three to five pairs of poles will usually provide sufficient margin.

An overcurrent or an undervoltage relay may be used to actuate the notching element. However, it may be difficult to set an overcurrent relay to discriminate between a hunting condition and an out-of-step condition. Also, the voltage dip at the machine terminals may not be enough to drop out a voltage relay positively. A more reliable scheme makes use of a duodirectional watt relay and an overcurrent relay, with their contacts connected in series as shown schematically in Figure 3. The alternate closing of the forward and reverse contacts of the watt relay with the overcurrent relay contacts closed will notch up the notching element.

The watt relay prevents the overcurrent relay, which may be set to less than full-load current, from notching up because of changes in load current. The object of the overcurrent relay is to prevent the watt relay from notching up during hunting. If the machine hunts during light load, the watt relay will follow the oscillations, but the current will normally not be high enough to operate the overcurrent relay. If the machine is carrying a heavy load the current element might pick up, but the power surges during hunting will normally not reverse in direction, and the watt relay would not close its reverse contacts. The combination thus provides reliable out-of-step protection except when the machine pulls out because of complete loss of field. A cylindrical-rotor machine without field excitation may operate as an induction

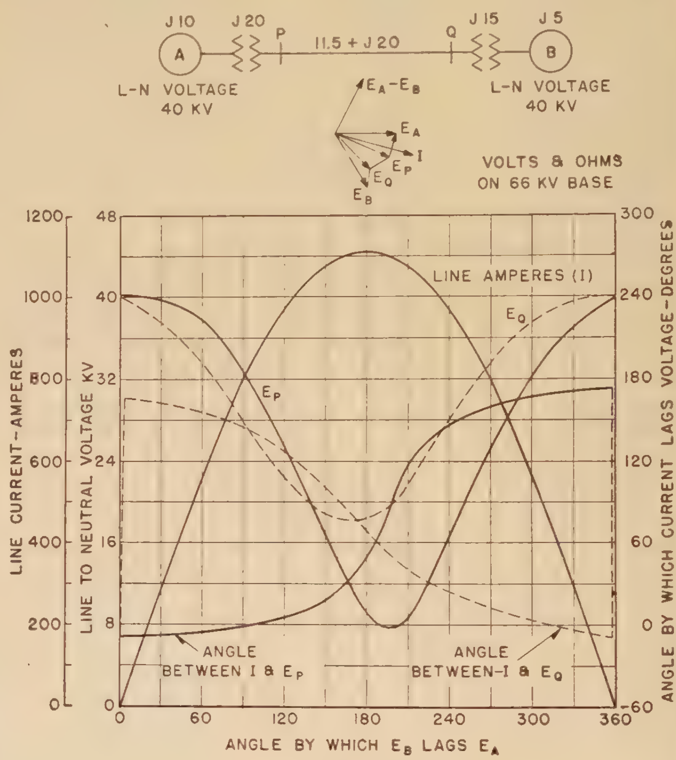

Figure 1. Behavior of the current and voltage at each end of a typical interconnection during a slip cycle

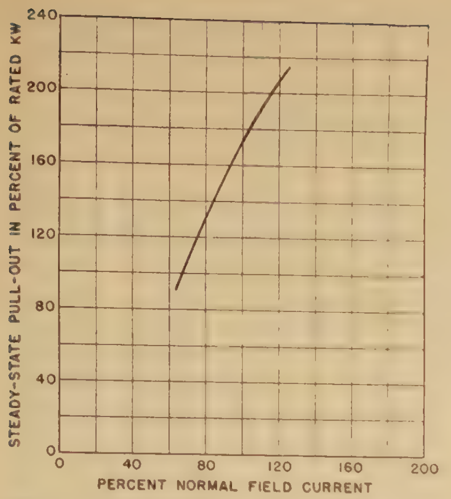

Figure 2. Effect of field excitation on steady-state pull-out of typical generator and system

generator at one or two per cent slip without reversal of power. The best protection against out-of-step operation from this cause is a field-failure relay.

Where there are several generators bussed together in a station, the out-of-step relays may have to be supplemented by a discriminating relay to indicate which machine is actually out of step. This might be a directional impedance relay used to indicate the direction of the electrical center, which would normally fall within any one machine that pulled out of step with others in a station. The difference in reactive current flow in the various machines might also be used as a discriminating factor. It is not known whether there are any such applications.

It is common practice to provide relays that will remove the field excitation from synchronous motors of moderate size when they pull out of step and reapply excitation at the proper time for resynchronizing. The power factor of the current flow into the motor has been used as an indication of loss of synchronism. In addition to power-factor relays, there are several relays designed especially to remove the field excitation on pull-out and reapply excitation for resynchronizing. One of these is the "slip-cycle impedance" relay which operates on the variations in impedance of the armature winding of the motor that occur during each slip cycle of out-of-synchronism operation. Another type of relay used for this purpose is the "field-frequency" relay which responds to magnitude and frequency of voltage generated in the field circuit. Also available is a "synchromatic" relay which responds to speed as a function of armature current and to time. There are many of these types of relays in successful operation.

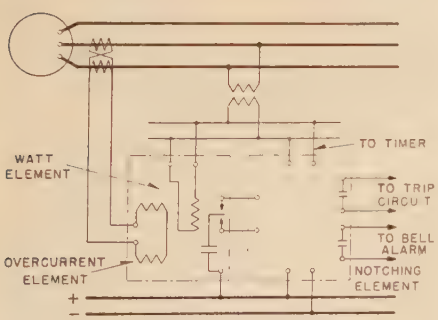

Figure 3. Out-of-step notching relay for protection of synchronous machines

Protection of Transmission Lines

Before Loss of Synchronism

Loss of synchronism between generating stations or between power systems is generally caused by some transient disturbance, such as a transmission line fault or a sudden load change. The ability of the interconnection to maintain synchronism during such a disturbance depends mainly on the speed of fault clearing and the angle between stations previous to the fault. The speed of fault clearing is fixed by the fault protective devices. However, the angular shift through the interconnection depends upon the load being interchanged and the impedance of the interconnection, both of which may vary. Either one or both of these quantities could serve therefore as a warning against possible loss of synchronism in case of a disturbance.

A watt relay can be used to sound an alarm or make a load correction when the load interchange reaches the allowable limit for a given impedance. However, load magnitude is not a reliable indication where the impedance is liable to change.

There is in service one group of relay installations intended to measure the angle between systems. The interconnections consist of both high- and low-voltage ties between systems. Stability cannot be maintained through the low-voltage tie in the event the high-voltage tie opens. A voltage relay is energized from a differential circuit supplied with voltage from each system. When the systems are over 60 degrees out-of-phase the relay operates to warn that something must be done immediately to relieve the condition.

Remote control by carrier-current telemetering offers possibilities of automatically giving an alarm or adjusting load interchange to conform to system conditions. There is one installation[3] where the receiver system phase angle is compared with the sending system phase angle by means of carrier current over a distance of 460 miles. The interchange is automatically controlled by this comparison of phase angle.

After Loss of Synchronism

When interconnected generating stations or power systems pull out of step, it is generally desirable to open the interconnection at such a point that system operation is least disturbed. However, all the fault protective relays in the interconnections are subjected to out-of-step current and voltage pulsations, and this may cause circuit breakers to be tripped at one or more undesirable locations. The usual function of out-of-step relays as applied to transmission lines is therefore to block undesirable tripping. They may also be used as out-of-step tripping relays to open rapidly the tie breakers at preselected locations during out-of-step conditions.

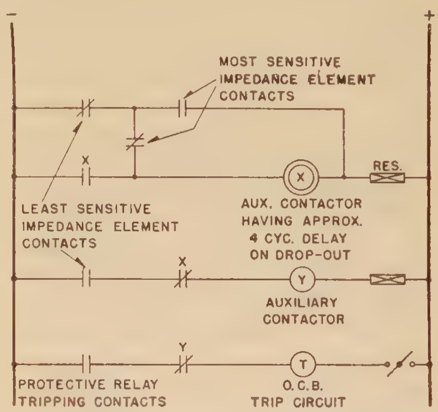

Figure 4. Out-of-step relay for blocking the trip circuits of fault-protection relays during out-of-step conditions

Relays operating on residual current or voltage for ground-fault protection are not affected during out-of-step, since the pulsations are balanced between phases. Also, parallel-line balanced-current relays and current differential relays, such as those used in a-c pilot-wire relaying, are not affected. However, overcurrent relays, undervoltage relays, distance-type relays, and directional comparison relays, such as those usually employed with carrier-current relaying, are susceptible to operation during out-of-step conditions. Distance relays can be provided with phase-angle characteristics to be less sensitive to system oscillations than to faults.

Most out-of-step relays for transmission-line protection make use of the gradual change in current or voltage or both in discriminating between a fault and out-of-step condition. Two elements differing in sensitivity enough to give a few cycles of time delay between pick-up as the two systems pull apart are used. Overcurrent elements can be used

if the interconnected generating capacity is essentially constant, so that the out-of-step current can be safely predicted. Impedance elements are better suited, however, because the ratio of voltage to current during out-of-step conditions varies over a greater range and is more nearly independent of generating capacity. The impedance elements may be the same as those used for fault protection, or they may be separate from the fault-protective relays. If a fault occurs within the protected zone, both elements operate substantially together and the out-of-step feature is made inoperative. However, if there is a relatively slow decrease in the measured impedance, such as that characterized by a system oscillation alone, one element operates before the other, and an auxiliary relay is operated either to trip or to block tripping as desired.

A typical out-of-step relay is shown schematically in Figure 4. The scheme can be made more positive in action by using three of the most sensitive elements, one per phase, with their contacts connected in series. It is then necessary only to discriminate between out-of-step conditions and three-phase faults. If necessary, the gradual change in angle between current and voltage as the systems pull out of step could also be used as an added discriminating factor.

Out-of-step blocking is more generally applied where carrier-current relaying is in use. This is probably because carrier-current relaying is commonly used on systems subject to instability and because out-of-step blocking can be readily adapted to carrier relaying. The blocking may be accomplished either by opening the trip circuit at each relay location during the out-of-step tripping impulse, or by transmitting a carrier signal through the slip cycle when an out-of-step condition is indicated.

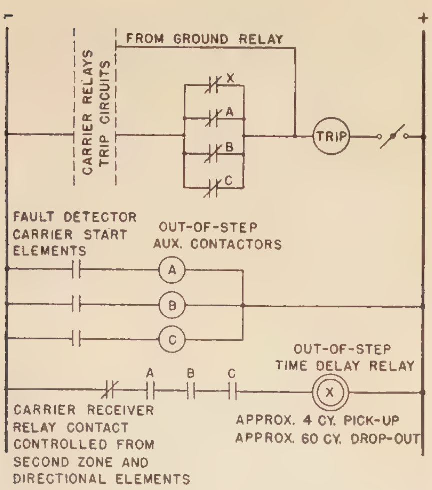

Figure 5. Out-of-step blocking combined with carrier-current relaying. Trip circuit is blocked during out-of-step period

A typical scheme of blocking the trip circuit is shown in Figure 5. The same distance-type elements are used for carrier control, for backup protection with the carrier out of service, and for indicating an out-of-step condition. Auxiliary contactors are used to block the tripping circuit during the period of a slip cycle if a three-phase-fault indication in the backup zone persists for approximately four cycles before the local carrier receiver relay closes its contacts indicating an internal fault. As indicated in Figure 1, impedance-type fault-detector elements would operate before the slip cycle advanced far enough to indicate an internal fault. This scheme can be made operative with the carrier out of service and the relays operating as distance type.

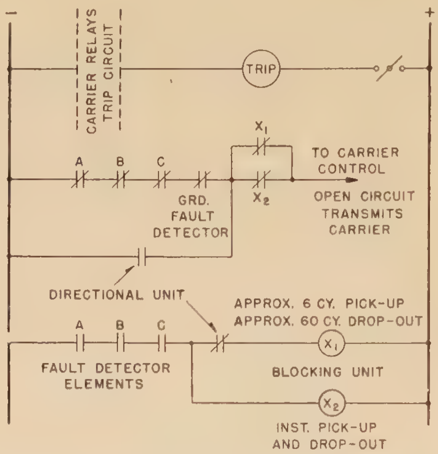

Figure 6. Another method of combining out-of-step blocking with carrier-current relaying. Carrier signal is transmitted to block tripping during out-of-step period

A typical scheme of blocking by transmission of carrier signal is shown in Figure 6. An auxiliary blocking unit causes a carrier signal to be transmitted during the out-of-step period when a three-phase fault indication in the nontripping direction persists for some six cycles at either terminal.

There are some installations where the elements of distance relays used for fault protection without carrier are also used for out-of-step blocking or tripping. This is usually done as shown in Figure 6. By means of a control switch the out-of-step relay can be set to either block tripping or to trip on the first out-of-step swing.

In some carrier-current installations high-speed tripping is sacrificed in an attempt to prevent tripping during out-of-step conditions. Other schemes in use transmit blocking carrier for a short time after an external fault has been cleared, in anticipation of a heavy system oscillation.

Operating Practices and Requirements

A questionnaire soliciting general comments on operating practices and requirements regarding out-of-step protection has been sent to a number of relay engineers throughout the country. Their replies have been used as a guide in preparing the text. In addition, a questionnaire of specific questions was sent to another group of relay engineers representing utilities having various types of interconnections. It is believed that their replies give a fair cross section of actual practices and requirements. The questionnaire read as follows:

1. Have any cases of synchronous-machine pull-out occurred on the system you represent?

2. What precautions, if any, are employed to recognize a condition such as sudden overload or loss of excitation that might cause a synchronous machine to pull out of step? Please explain the principle of the scheme of protection used and its operating record.

3. Which of the following do you consider the most desirable in case of the loss of excitation on a major generator:

(*a*). Sound an alarm.

(*b*). Trip the machine off the line.

(*c*). Automatically transfer to an emergency source of excitation.

4. What scheme of protection, if any, is used to detect that a machine has pulled out of step? What is its operating record?

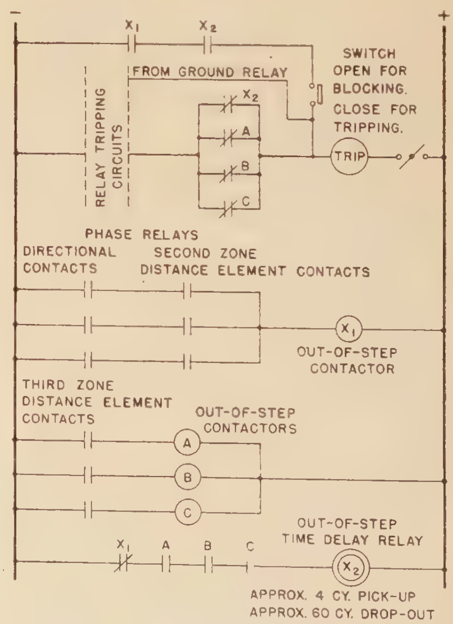

Figure 7. Out-of-step blocking or tripping combined with fault-protection relays without carrier current

5. Which of the following do you consider the most desirable in case a synchronous machine pulls out of step?

(*a*). Sound an alarm.

(*b*). Trip the machine off the line.

6. Have any cases of system instability, that is, instability between systems or between generating stations, been experienced?

7. Have there been any cases where out-of-step or severe system swings have caused line relays to operate?

8. What scheme, if any, is used to block tripping of line relays during out-of-step or to separate the system at a selected point? Is the scheme used with or without carrier current? What is its operating record?

9. Which of the following schemes of operation do you consider most desirable?

(*a*). Provide no out-of-step blocking and let the line relays trip where they may.

(*b*). Provide blocking of all relays that are liable to trip during out-of-step with the thought that stability will be regained.

(*c*). Provide blocking of all relays that are liable to trip during out-of-step except those at one selected location where out-of-step tripping is least objectionable.

10. Do you consider it desirable to keep the out-of-step blocking devices entirely separate from the line relays or to incorporate them in the line-relaying scheme if possible?

11. Miscellaneous comments.

A summary of the replies to the questionnaire is given in Table I.

General Conclusions

1. Practically all operating companies have had synchronous machines pull out of step because of loss of excitation or some other disturbance.

2. It is not general practice to provide generators with means of detecting disturbances that might cause the machine to pull out of step. Some have reverse-current or undercurrent relays in the field circuit, and at least one company protects against inadequate excitation by measuring the reactive current taken by the generators. Quite a few provide partial protection by means of overcurrent relays, although it is recognized that this does not provide positive protection against out-of-step operation. Large synchronous motors and synchronous condensers are quite generally supplied with protection against loss of field.

3. There is a divided opinion as to whether an alarm should be sounded in case a major generator loses excitation or whether the machine should be tripped off the line. Most operators seem to prefer tripping.

4. It is not general practice to provide special devices for detecting when generators pull out of step. Large synchronous motors and synchronous condensers are often provided with some degree of protection by overload relays. Some are provided with out-of-step relays.

5. There is a divided opinion as to whether an alarm should be sounded when a synchronous machine pulls out of step or whether it should be tripped off the line. The majority seem to favor tripping. There is a feeling that if a generator pulls out of step because of loss of excitation it will not regain synchronism without excessive disturbance if excitation is restored. It is generally felt that a machine will be seriously damaged if it is not tripped immediately. However, there have been cases where turbine generators have pulled out of step because of loss or reduction of excitation, have run for a minute or two as induction generators, and then pulled back into step when excitation was restored. Inspection showed no apparent damage.

6. Practically all utilities, except those consisting of steam stations connected rigidly together electrically, have experienced system instability.

7. Most utilities have experienced undesirable operation of fault-protective relays as a result of system instability.

8. Quite a number of utilities attempt either to block line relays from tripping because of out-of-step conditions or to set the relays so that tripping will occur at a preselected point. Out-of-step blocking in conjunction with carrier relaying is the method most commonly used.

9. Most utilities would prefer to provide blocking of all relays that are liable to trip during out-of-step except those at one selected location where out-of-step tripping is least objectionable. Some use out-of-step relays for tripping where fault-protection relays cannot be depended on to trip during out-of-step operation.

10. There is a divided opinion as to whether out-of-step devices should be kept entirely separate from the line relays or whether they should be incorporated in the line-relaying scheme if possible. There may be applications where the settings of the elements for out-of-step protection should be different from those for fault protection. In some cases there is an advantage in having the out-of-step devices separate from the line relays.

11. Most operators agree that, where possible, consideration should be given to strengthening interconnecting ties between systems and to decreasing fault clearing time in an effort to prevent instability. However, this cannot always be economically justified. Also, the war has made it necessary to explore every possibility to attain maximum utilization of existing equipment. Out-of-step blocking and selective tripping provide one effective way of doing this.

12. There are out-of-step devices and methods available that will meet most requirements if applied properly. However, operating experience with some of them is limited. There is an apparent need for further investigation of the action of out-of-step relays during certain conditions. Among these are:

(*a*). A prolonged fault during out-of-step.

(*b*). The closing of a circuit breaker on a system out of step.

(*c*). Out-of-step operation through a long high-voltage line where charging current is to be reckoned with.

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Distance Relay Protection for Subtransmission Lines Made Economical

L. J. AUDLIN
MEMBER AIEE

A. R. van C. WARRINGTON
ASSOCIATE AIEE

Synopsis: Less important transmission lines are usually protected with directional overcurrent relays. Much better protection and consequently improved service would be provided by modern high-speed distance relays but at several times the cost. What is required is a distance relay arrangement considerably less expensive than the relays now used on high-voltage transmission lines but with as nearly as possible the same characteristics. Such an arrangement is now available. The paper describes successful tests in the laboratory and on an actual system in this country with both natural and staged faults.

The equipment consists of a single high-speed reactance relay which is enabled to protect all three phases by being automatically switched to the correct potential and current by a phase selector relay.

LESS important transmission lines are usually protected with directional overcurrent relays or induction disk-type impedance relays. On most of such subtransmission circuits there are stations where, in order to obtain selectivity, the relays have to be set for perhaps two seconds, and occasionally selectivity between two relays cannot be obtained at all because under one condition one relay has more current and under another condition the other has more. Better selectivity can be obtained with distance relays, but they are more expensive than overcurrent relays.

The modern high-speed induction-cylinder-type reactance relay is extremely accurate and easy to set. It is more expensive than either the overcurrent relay or the impedance relay of the induction-disk type, hence its use has generally been confined to high-voltage transmission lines. What is required for subtransmission lines is a relay with substantially the characteristics of the high-speed reactance relay at the price of the directional overcurrent relay. Although this is not yet literally possible, a very good compromise can be obtained by using one reactance relay, instead of the usual three, and a selector relay to connect it to the proper currents and potentials.

While, in general, stability is not the problem on subtransmission circuits that it is at the higher voltages, there are cases where it is desirable to obtain fast clearing times, such as on circuits between a large generating station and a load area; also, where a line may be burned down unless promptly cleared. In the arrangement to be described the selector relay operates in four cycles and the distance relay in one cycle for nearby faults making a total of 5 cycles on a 60 cycle system.

It is seldom that more than one of the distance relays in the three phases operates on a fault because only one of the line-to-line potentials is affected in line-to-line faults. In balanced three-phase faults all three relays may operate, but only one is necessary to effect tripping. Consequently, for less important transmission lines and those where one cycle relay operation is not required, it is permissible to use a single distance relay and to connect it to the faulted phases by a selector relay.

Compared with directional overcurrent relays for phase faults the new scheme is easier to apply and gives complete selectivity combined with high-speed tripping at only a moderate increase in cost. Compared with conventional distance relays the new scheme effects a saving in critical materials and labor, since one distance relay and an auxiliary relay replaces three distance relays. This in turn reduces investment, panel space, and maintenance.

Historical

In 1934 a paper[3] was presented before the Institute describing different methods of controlling distance relays with selector relays so that one set of distance relays could take care of both phase and ground faults or one relay could protect three phases against either phase or ground faults. Later 72 induction-disk-type reactance relays were installed, normally connected for phase faults, and with provision for switching to wye potentials, and so forth, for ground faults. A report of satisfactory operation was received, and twenty-four more were installed on the same system.

Selector relay schemes were widely used in Europe, but they did not become popular in this country. One reason for this may have been that the distance measurement with the selector schemes then available was not as accurate as with the full set of relays, particularly on ground faults; another may have been that the minimum time was about half a second. It is the purpose of this paper to describe a modern arrangement of distance and selector relays in which the same selectivity and accuracy is obtained as with a full set of distance relays and the minimum time is 0.08 second.

In one scheme a single distance relay protects against phase faults. In the other a single distance relay protects against ground faults. Both have been thoroughly tested in the laboratory, on the artificial transmission line, in life tests, and by staged faults on actual systems. Both are in service on lines in this country.

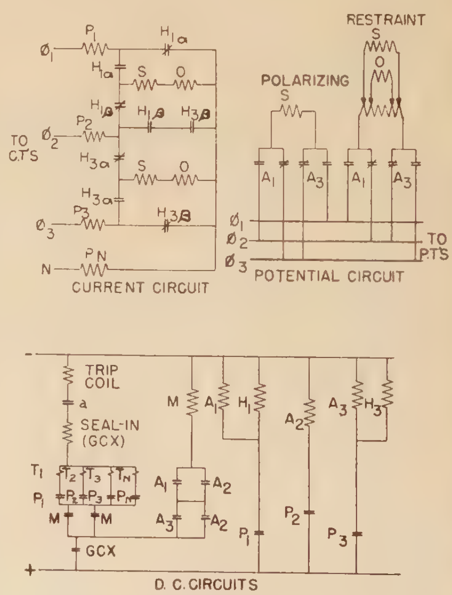

Figure 1. Schematic diagram for phase faults

$P_1P_2P_3P_N$—Overcurrent fault detectors in the three phases and residual circuit

$A_1A_2A_3$—Auxiliary relays controlled by $P_1P_2P_3$

H_1H_3—Current transfer relays. α and β refer to their two sets of transfer contacts

S and O—Starting and ohm units of the reactance relay

M—Relay for delaying tripping 0.025 second

$T_1T_2T_3T_N$—Targets indicating circuit involved in fault

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L. J. AUDLIN is relay supervisor in the power control department of the Central New York Power Corporation, Syracuse, N. Y. A. R. VAN C. WARRINGTON is design engineer, relay section, switchgear department, General Electric Company, Philadelphia, Pa.

The kindness of the Duquesne Light Company and of the Central New York Power Corporation in arranging for the test was greatly appreciated. The engineering and operating staffs of the two companies are to be commended for their efficient planning and execution of the tests.

The relay provides three zones of operation, giving the well-known stepped time-distance characteristic which has been described in previous papers. Faults up to 90 per cent of the distance to the next station are cleared instantaneously; faults in the neighborhood of the next station are cleared after a delay sufficient to allow the proper breaker at that station to open; more remote faults are cleared in a still longer time. These three zones are adjustable in distance by means of an internal tapped transformer; and all but the shortest range, which operates in about one cycle, are adjustable in time.

Upon the occurrence of a fault in the tripping direction the relay operation is initiated by a directional impedance unit called the starting unit. The zone is decided by the ohm unit, which measures the reactance of the line between the relay and the fault, and the time is controlled by a clockwork timer, self-wound by a d-c solenoid.

The starting and ohm units are of the induction-cylinder type which is well known for its efficiency and a steady torque. The ohm unit is accurate within plus or minus two per cent over a wide range of current and power factor. The use of reactance as a basis of distance measurement minimizes error caused by arc resistance which is particularly important in ground faults.

For phase faults the reactance relay is supplied with the potential between the faulted conductors and the vectorial difference of the current flowing in them. With this excitation the relay will measure the same distance for three-phase, phase-to-phase, and double-ground faults. For ground faults the reactance relay has the phase-to-neutral potential of the faulted conductor and the current in the conductor plus a predetermined portion of the residual current. With the proper amount of residual current the relay will measure substantially the same distance regardless of changes in system connections.

Protection Against Phase Faults

A high-speed reactance relay is controlled by a selector relay. Figure 2 shows the external appearance of these relays.

The operating characteristics of the reactance relay are the same as those of the standard *GCX* relay except that shorter lines can be protected because the potential circuit is not continuously energized and hence can be short-time rated.

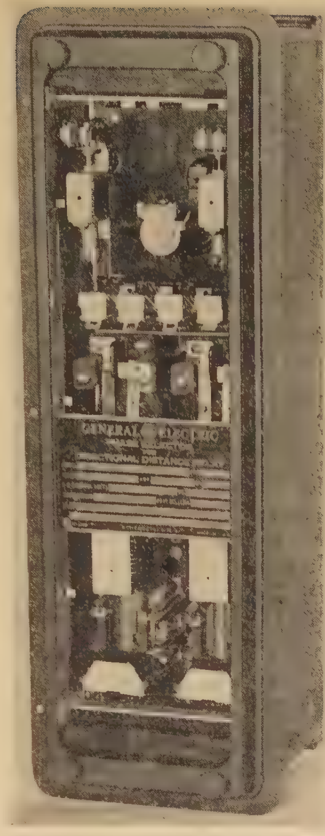

Figure 2a (left). Selector relay for phase faults

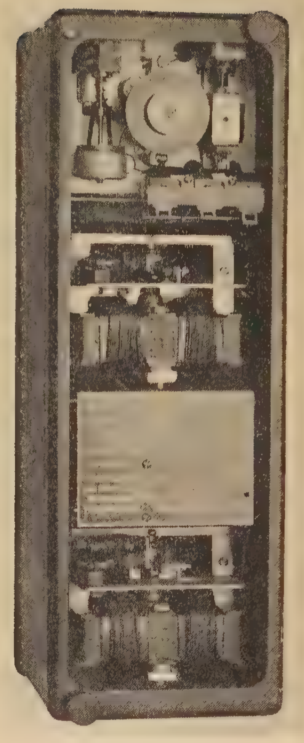

Figure 2b (right). Type GCX high-speed reactance relay

The selector relay contains two calibrated instantaneous overcurrent units (P_1 and P_3 in Figure 1) in phases 1 and 3, which detect the fault and determine the phases involved. These overcurrent units control auxiliary relays A_1 and A_3 respectively which apply the line-to-line potential between the faulted phases to the restraining coils and the quadrature potential to the polarizing coil of the reactance relay. They also energize the proper current transfer relay, H_1 or H_3, and set up the correct tripping circuits for target indication. There are two smaller overcurrent units, P_2 and P_N, in phase 2 and the residual circuit, which are for target indication. There are two current transfer units H_1 and H_3 which connect the current coils of the reactance relay to the current transformers of the faulted phases. The selector relay also contains a time-delay unit M which delays tripping slightly so as to ensure proper selection by the reactance relay unaffected by an external fault changing its character. There are four targets marked phase 1, phase 2, phase 3, and ground.

The currents and potentials supplied to the reactance relay are shown in Table I.

The overcurrent units are of standard design and the current transfer units have high-pressure overlapping contacts which ensure that the current-transformer circuit will never be opened.

Table I

Type of Fault	E Restraint (Ohm Unit and Starting Unit)	I	E Polarizing (Starting Unit)	X Measured*
$\phi_1-\phi_2$ or $\phi_1-\phi_2-G$	E_{12}	I_1-I_2	E_{23}	$2X_p$
$\phi_2-\phi_3$ or $\phi_2-\phi_3-G$	E_{23}	I_2-I_3	E_{31}	$2X_p$
$\phi_3-\phi_1$ or $\phi_3-\phi_1-G$	E_{13}	I_1-I_3	E_{21}	$2X_p$
$\phi_1-\phi_2-\phi_3$	E_{13}	I_1-I_3	E_{21}	$2X_p$

* X_p is the positive sequence reactance.

Ground-Fault Protection

The relays are similar to those used for phase faults, but wye potentials are used, and residual current is supplied to the second current coil instead of minus the current from the next lagging phase. Residual current is also supplied from any parallel lines in order to compensate for mutual coupling.

The ground-reactance relay differs from the phase-reactance relay only in the addition of a capacitor to give the starting unit maximum torque with residual current 45 degrees lagging the residual potential. Figure 3 shows how the connections of the ground-selector relay differ from those of the phase-selector relay which was shown in Figure 1.

Type of Fault	E Restraint (Ohm Unit and Starting Unit)	I	X Measured
ϕ_1-G	E_1	I_1+KI_{res}	$2X_p$
ϕ_2-G	E_2	I_2+KI_{res}	$2X_p$
ϕ_3-G	E_3	I_3+KI_{res}	$2X_p$

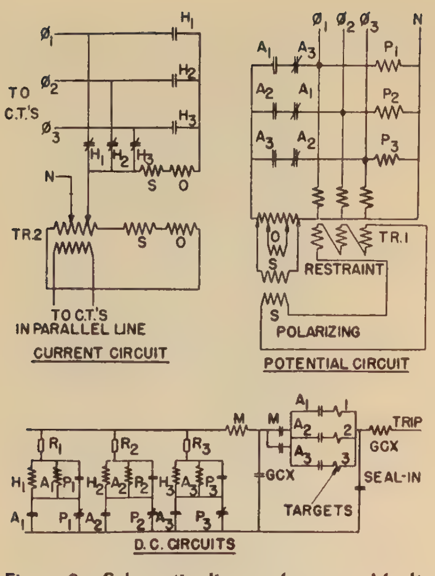

Figure 3. Schematic diagram for ground faults

$P_1P_2P_3$—Undervoltage fault detectors in the three phases
$A_1A_2A_3$—Auxiliary relays controlled by $P_1P_2P_3$
$H_1H_2H_3$—Current transfer relays
$R_1R_2R_3$—Resistors
TR 1—Wye-broken delta auxiliary potential transformer
TR 2—Residual auxiliary current transformer

Three undervoltage units energized with wye potential are used for detecting ground faults on systems grounded through impedance. Figure 5 shows how a single-phase ground fault on an impedance-grounded system displaces the neutral so that the faulted phase gets low voltage whereas the other two get above-normal voltage, so that the selection of the faulty phase is very definite. On solidly grounded systems either undervoltage or overcurrent fault detectors can be used, depending upon the distance from a grounding point.

The undervoltage units are set to drop out at 90 per cent of normal potential. The reactance relay is normally de-energized; when a fault occurs the undervoltage unit P in the faulted phase drops out energizing an auxiliary relay A and a current transfer relay H. The relay A seals around the contacts of P and applies the potential of that phase to the reactance relay as shown in Figure 3 while the transfer unit H connects the relay-current coils to the current transformer of the same phase, so that the relay is now ready to measure the distance to the fault. When the fault has been cleared and line voltage restored to normal, the P units pick up and short-circuit the coils of the H and A units so that they reset.

The reactance relay does not trip directly but through an auxiliary relay M with a slight time delay; this is in order to give the reactance relay a chance to reset if an external fault should change its character. For example, if an external single-phase fault should blow into a second phase, the reactance relay may close its contacts, because the additional current between the conductors may make the line reactance appear to be lower than the ohm unit setting. This condition will be very brief because the operation of the second current-transfer relay will by-pass the interphase current; the time-delay relay requires that the reactance-relay contacts remain closed for one cycle before it will trip the breaker; hence the reactance-relay contacts will reset before the auxiliary relay can pick up and trip. During this double-ground fault condition the selector relay supplied both current windings with residual current and the potential winding with the leading of the two wye potentials involved in the fault. This ensures underreaching on double-ground faults so that the relay acts only as a back-up for phase relays.

The current coils of the reactance relay are all double-wound. One winding is supplied with residual current and the other with phase current controlled by the phase selector relay. The restraining potential coils of the ohm unit and starting unit have phase-to-neutral potential controlled by the selector relay. The polarizing or directional potential coil of the starting unit has residual potential which is supplied by auxiliary potential transformers connected wye-broken delta. In order to obtain correct measurement the potential should be supplied by three potential transformers in wye-wye grounded on the high side.

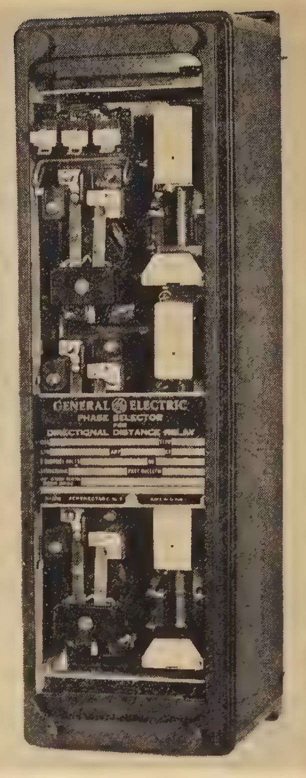

Figure 4. Selector relay for ground faults

Targets

The usual targets are provided in the reactance relay to indicate the zone or time of operation. In the selector relay for phase protection four targets are provided for the three phases and ground.

The selector relay for ground faults has three targets. A ground target is not necessary, because the relay does not work unless the fault involves ground.

Tests

The tests were of three kinds: laboratory tests, including life tests to check the effectiveness of the "switching"; tests on the artificial transmission line to check the sequence of operation and the accuracy of distance measurement; and, finally, natural and staged tests and operating experience on an actual transmission line.

The point of greatest interest for most engineers is the idea of switching the current circuit. In the standard reactance relay, the potential and d-c circuits have been switched from zone 1 to zone 2 con-

Table II

Test Number	Fault Location	Type	Fault	Location	Phase Targets	Zone Targets
1	Ilion	Solid	1–2–G	5% beyond zone 1 setting	1–2–G	2
2	Ilion	Solid	1–2–G	5% inside zone 1 setting	1–2–G	1
3	Ilion	Solid	2–3–G	5% inside zone 1 setting	2–3–G	1
4	Ilion	Solid	3–1–G	5% inside zone 1 setting	3–1–G	1
5	Ilion	Solid	3–1–G	5% beyond zone 1 setting	3–1–G	2
6	Ilion	Solid	1–G	5% inside zone 1 setting	—	—
7	Ilion	Solid	3–G	5% inside zone 1 setting	—	—
8	Ilion	Solid	1–2–3–G	5% inside zone 1 setting	1–2–3	1
9	Ilion	Solid	1–2–3–G	5% inside zone 1 setting (current reversed in relay)	—	—
10	Washington Street	Solid	1–2–3–G	On outgoing side of bus (current reversed in relay)	—	—
11	Washington Street	Solid	1–2–3–G	On outgoing side of bus	—	—

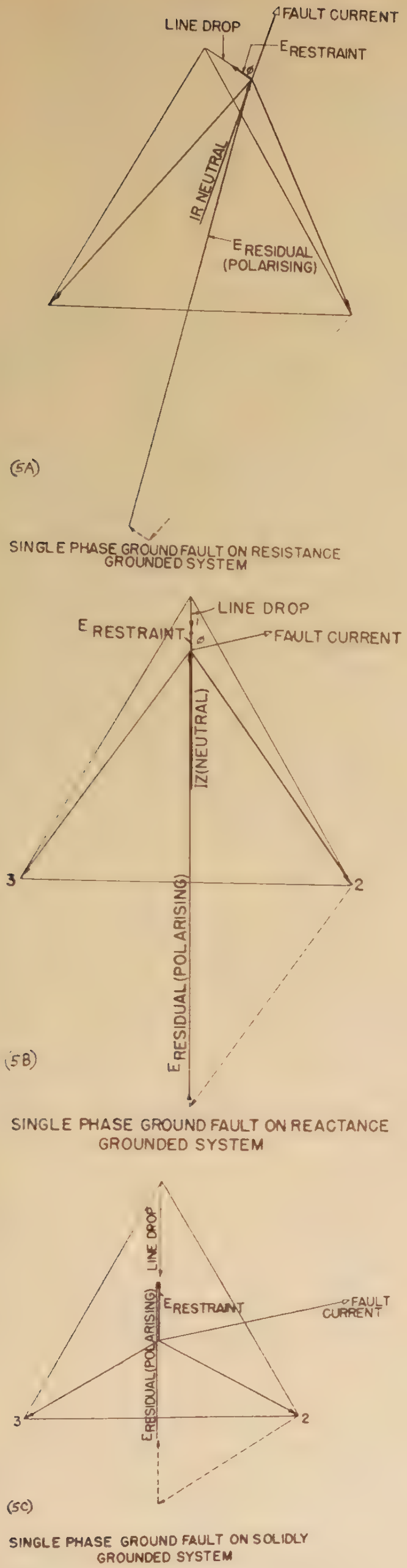

Figure 5. Vector diagram of ground fault

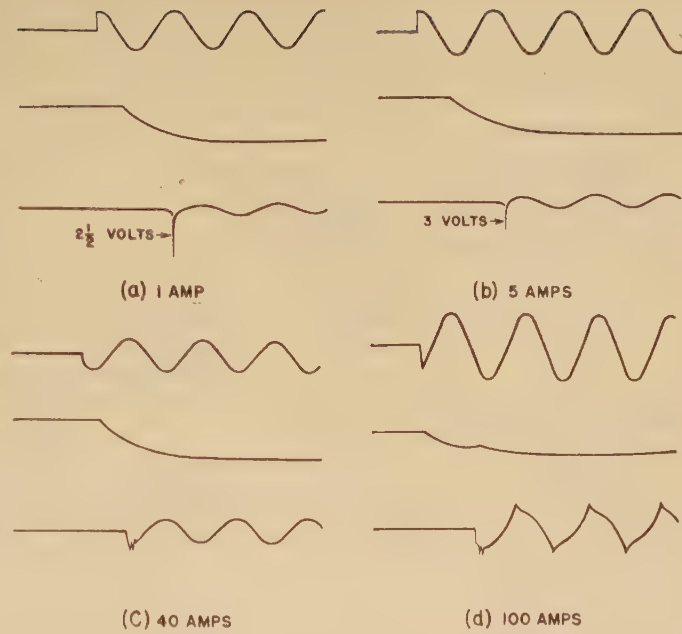

Figure 6. Oscillograms of current transfer relay operation

The top trace is the current in the winding of the current transformer. The middle trace is the current in the coil of the current transfer relay. The bottom trace is the voltage across the current windings of the reactance relay and the contacts of the current transfer relay

nections for the last eight years without trouble, and it is actually easier to switch current than potential because the current transformer will provide a high enough potential to break down an insulating film on the contacts. With contacts of adequate size, overlap, and pressure, switching current is no less reliable than switching potential, and the contacts require less maintenance.

(a). Laboratory Tests

Oscillograms (Figure 6) taken at 1, 5, 40, and 100 amperes showed smooth transition of current. The voltage across the contacts was negligible at the higher currents; at five amperes there was a peak of about three volts magnitude which lasted only about 0.00002 second. After a continuous life test of 5,000 operations at 100 amperes, the contacts were still in good shape; oscillograms showed some peaks up to 11 volts, but of less than one millisecond duration. A life test of 500 operations with zero overlap marred the contacts but did not interfere with their operation. A further life test with $^1/_{32}$-inch negative overlap, that is, break before make, caused pitting of the contacts, but no failure, although there was a large amount of energy in the arc because of 100 amperes flowing through $^1/_{32}$ inch of air. This was obviously worse than any possible condition of deteriorated adjustment, so it was felt that the design was very satisfactory.

In all these life tests, the circuit was arranged to close 100 amperes on the *a* contacts for three seconds and then to transfer back to the *b* contacts at three amperes for 15 seconds. These conditions are very much more severe than any service duty because of the heat that is accumulated in the contacts.

(b). Artificial Transmission-Line Tests

In these tests, the relay was applied to lines of different lengths, and the tap settings of the reactance relay for the borderline between zone 1 and zone 2 operation were checked for different conditions of generator and load to see if any variations in balance point occurred which could be blamed on the selector relay. In addition, tests were made in which the type of fault was changed (for instance from phase 1–ground to phase 1–phase 2–ground) to determine if, during the transition, any incorrect tripping would result from momentarily having the wrong potential or current on the distance measuring elements. Using all possible fault combinations it was impossible to cause incorrect operation.

(c). System Tests on Phase Relays

The reactance and selector relays for phase faults were installed last year on the Washington Street–Ilion line at the Washington Street substation on the 44-kv system of the Central New York Power Corporation. This circuit has a calculated resistance of 7.37 ohms and a calculated reactance of 10.75 ohms, which corresponds to 1.61 ohms secondary reactance. The relay measures $2X_p$ which is 3.22 ohms; zone 1 (instantaneous) was set to reach ten per cent short of Ilion, 2.86 ohms, and zone 2, 3.45 ohms. The time settings were one-half second and two seconds for zone 2 and zone 3.

There have been two faults on this circuit during electrical storms. The first case of trouble was a one-wire-to-ground fault approximately 90 per cent of the distance from Washington Street to Ilion. The relays did not operate on this disturbance, which was correct.

When the second fault occurred the relays operated and showed 1–2–3–G and zone 1 targets, indicating that a fault had probably started two-wire-to-ground and then involved the third phase. The

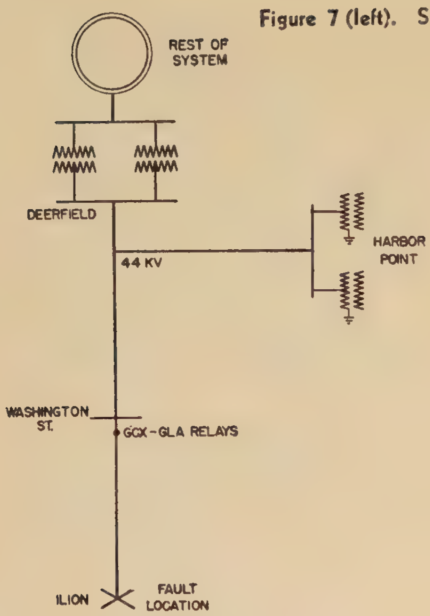

Figure 7 (left). Single line diagram of test circuit for phase relays

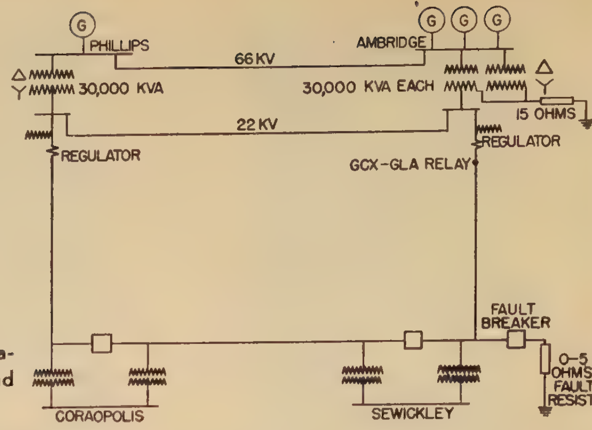

Figure 8 (right). Single line diagram of test circuit for ground relays

fault was approximately 25 per cent of the distance from Washington Street to Ilion. A simultaneous three-phase-to-ground fault would not show a *G* target, because the fault current would be practically balanced in the three phases and would give negligible residual current.

Several cases of trouble have occurred on other circuits in the next bus section or in the bus section back of the relay where fault current was reversed in the relay and the relay scheme functioned correctly; that is, did not operate.

For the purpose of the tests a portion of the 44-kv system was reconnected temporarily as in Figure 7. The short-circuit phase current was supplied by two 15,000-kva, 110,000 wye to 44,000 delta, transformer banks connected to the 110,000-volt system at the Deerfield substation in Utica. The calculated three-phase short circuit is 1,178 megavolt-amperes on the Deerfield 110-kv bus. Two grounding transformers, located at Harbor Point substation in Utica, supplied the ground current.

Solid faults were placed on the Washington Street–Ilion line between the line disconnect and the oil circuit-breaker at Ilion with the circuit de-energized. The faults were initiated on the system from the Washington Street 44,000-volt bus by closing the circuit breaker at the Washington end. The 44,000/110-volt potential transformers connected to this bus supplied potential to the relays and a *PM*-13 oscillograph.

Instead of moving the fault location the relay setting was changed five per cent each way to simulate faults just inside and just outside of the instantaneous zone. See Table II.

The relay potential in the last two tests was so low that indeterminate action was expected, but actually, in test 11 the relay moved slowly toward the tripping contacts and would probably have tripped if the back-up relay had not tripped first.

(d). System Tests on Ground Relays

The relays were tested on the 22-kv system of the Duquesne Light Company near Pittsburgh. The principal purpose of this was to determine the effect of load current and ground resistance on the accuracy of ground-distance relays on a system grounded through a resistance of roughly one ohm per kilovolt. The results of the tests will be published later when complete and edited. The present description of the tests will be limited to reference to the operation of the selector relay.

The tests were made at the Ambridge station on the line to Sewickly where the faults were applied as shown in Figure 8. Sewickly was also fed from Phillips in the other direction via Coraopolis. Between each source of power and the fault was a voltage regulator so that the phase and magnitude of the current transfer could be accurately controlled over 360 degrees range. The resistance of the faults at Sewickly was varied between zero and five ohms, and the balance points were found for various conditions to determine the effect of load in combination with fault resistance.

Oscillograms were taken of the phase current, the residual current, the phase potential, the residual potential, the current and potential supplied to the reactance relay, and the tap current. For each condition, the setting of the relay was calculated for the borderline between zone 1 (instantaneous) and zone 2 (intermediate time). If the relay tripped in zone 1, the potential tap setting was increased one or two taps until it showed a zone 2 target, and the balance point was therefore between the tap which gave a zone 1 target and the tap that gave a zone 2 target.

The balance point was checked with the selector operating normally and with its contacts held manually in the positions they would assume during the fault. In no case was there any difference between the balance points.

Conclusions

The combination of selector relay and distance relay thus provides the accuracy, selectivity, and ease of application of the *GCX* relay and for the first time makes distance relay protection economical for subtransmission lines where the expense of a full set of distance relays is not justified, and five-cycle operation is satisfactory.

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A Method for Correlating Duty-Cycle Tests on Solenoids

J. E. RYAN
NONMEMBER AIEE

A SOLENOID is customarily rated in terms of the minimum pull exerted at any point within its rated stroke when a certain fraction of its nominal voltage is impressed on the coil. Furthermore, when full nominal voltage is applied continuously, the temperature rise of the coil must not exceed a certain value, depending on the class of insulation used and the maximum ambient temperature.

In many applications, however, solenoids are not energized continuously but perform their function at more or less regular intervals with rest periods between operations. With a-c solenoids operating on such a duty cycle, two influences are present, one tending to decrease the heating, the other tending to increase it. Because it is energized only a fraction of the time, the solenoid, with its plunger seated, develops less heat per second than when continuously energized. On the other hand, the power input during the brief time when the plunger is in motion is many times greater than when the latter is seated. The total energy thus expended at each operation depends on the load and the stroke. Thus, with a given load and stroke, it is possible to operate the solenoid at such intervals that the additional energy expended during the pickup periods exactly compensates for the reduced heating in the seated position. The coil temperature rise will then be the same as in the continuously energized case, and the solenoid may be considered as operating at its full duty-cycle rating for the particular load, stroke, and per cent time energized in question.

The relations between load, stroke, per cent time energized, operations per unit time, and temperature rise are best determined experimentally, but the procedure is tedious if the operation frequency must be varied by trial on each test until the allowable temperature rise is reached. Moreover, the results are then applicable only in cases where that particular temperature rise limit is prescribed. With the test and correlation method hereinafter described, a minimum of tests provide data from which the temperature rise under any combination of the operating variables may be predicted.

The analysis underlying the correlation method is given first, followed by a description of the test and correlation procedure. The symbols used are defined in the appendix.

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J. E. RYAN is with the General Electric Company, Schenectady, N. Y.

Analysis

The static pull curve of a typical a-c solenoid is shown in Figure 1. If the plunger starts its closing motion at a gap s, the kinetic energy possessed by the moving parts of the solenoid and its load at gap x will be

$$E_x = \int^{s} (f-L)dx = \frac{Wv^2}{2g} \tag{1}$$

$$\text{whence } v = -\sqrt{\frac{2gE_x}{W}} \tag{2}$$

Since

$$dt = \frac{dx}{v}, \qquad T = \int_s^0 \frac{dx}{v} = \sqrt{\frac{W}{2g}}\int_0^s \frac{dx}{\sqrt{E_x}} \tag{3}$$

which may be written as

$$T = \sqrt{W}F_1(L, s, x) \tag{4}$$

The heat input to the coil during the plunger travel is

$$H_c = \int_0^T P_c dt \tag{5}$$

Figure 2 shows a typical relation between P_c and x, taken from static tests. Actually, when the plunger moves rapidly, P_c may also be a function of those factors which affect the speed, namely, L, W, s, x. Then, using equation 4 in 5

$$H_c(L,W,s) = \sqrt{W}\int^0 P_c(L,W,s,x)dF_1 \times (L,x,s) \tag{6}$$

where the integration limits are on the variable x. Hence

$$H_c(L, W, s) = \sqrt{W}F_2(L, W, s) \tag{7}$$

Similarly

$$H_i(L, W, s) = \sqrt{W}F_3(L, W, s) \tag{8}$$

During one operation of the solenoid, the total heat produced in the coil is then

$$H_c + \left(\frac{A}{N} - T\right)P_{cs} \tag{9}$$

where $(A/N-T)P_{cs}$ is the heat produced while in the seated position. It follows that the average power in the coil is

$$\overline{P}_c = \frac{H_c + \left(\frac{A}{N} - T\right)P_{cs}}{\left(\frac{1}{N}\right)} = (H_c - P_{cs}T)N + P_{cs}A \tag{10}$$

Similarly,

$$\overline{P}_i = (H_i - P_{is}T)N + P_{is}A \tag{11}$$

These average power values may be related to the coil temperature rise by considering the equivalent thermal circuit of the solenoid shown in Figure 3. If the thermal resistances are assumed independent of temperature or temperature difference, it is evident that the temperature at 0, representing the average condition in the coil, will be a linear function of $\overline{P}_c$ and $\overline{P}_i$ of the form

$$\theta = F_4(R_{ca}, R_{ci}, R_{ia})\overline{P}_c + F_5(R_{ca}, R_{ci}, R_{ia})\overline{P}_i \tag{12}$$

Actually, the various R's are not strictly constant even during a single operation of the solenoid. When the plunger is extended, the inner surfaces of the coil and the plunger itself are more effectively cooled than when the latter is seated. If it can be assumed that the heat transfer through each of the resistances is improved by a factor $(1+K_1S)$ whenever the plunger is extended, the average resistances for a complete operation cycle are:

$$R_{ca} = \frac{R_{ca}'}{1+K_1S(1-A)}; \quad R_{ci} = \frac{R_{ci}'}{1+K_1S(1-A)}; \quad R_{ia} = \frac{R_{ia}'}{1+K_1S(1-A)} \tag{13}$$

Hence equation 12 becomes

$$[1+K_1S(1-A)]\theta = F_4(R_{ca}', R_{ci}', R_{ia}')\overline{P}_c + F_5(R_{ca}', R_{ci}', R_{ia}')\overline{P}_i = k_c\overline{P}_c + k_i\overline{P}_i \tag{14}$$

Substituting equations 10 and 11 into 14 there results

$$[1+K_1S(1-A)]\theta = [k_c(H_c - P_{cs}T) + k_i(H_i - P_{is}T)]N + [k_cP_{cs} + k_iP_{is}]A$$

or

$$\theta = \frac{F_6(L, W, s)N + K_2A}{1+K_1S(1-A)} \tag{15}$$

K_1, F_6, and K_2 may be determined as described in "Determination of Constants." Having done this, θ may be

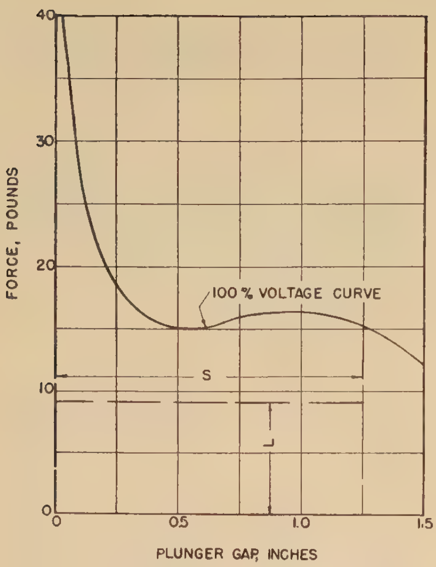

Figure 1. Pull curve of a typical a-c solenoid

evaluated from equation 15 for any combination of L, W, S, A, and N.

Determination of Constants

(A). K_2

The solenoid is energized continuously at full voltage until steady temperature rise is attained. According to equation 15, the coil rise θ is then

$$\theta = K_2A = K_2 \tag{16}$$

since $A = 1.0$ and $N = 0$

(B). F_6 AND K_1

Equation 15 may be written in the form

$$[1+K_1S(1-A)]\theta - K_2A = F_6(L, W, s)N \tag{17}$$

which can be of the straight-line form

$$y = mx$$

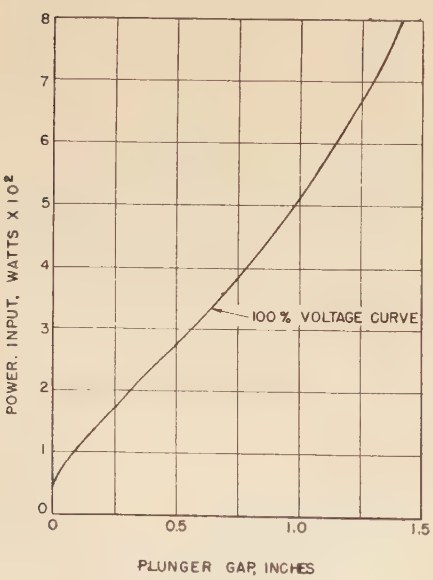

Figure 2. Power input versus plunger gap for a typical a-c solenoid

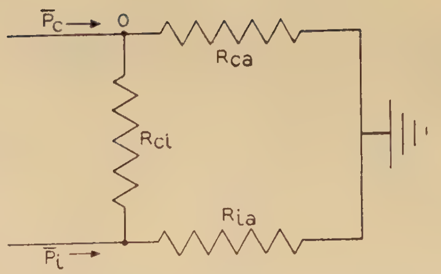

Figure 3. Thermal circuit of an a-c solenoid

if

$$y = [1+K_1S(1-A)]\theta - K_2A$$
$$x = N$$
$$m = F_6(L, W, s) \tag{18}$$

Thus a plot of $[1+K_1S(1-A)]\theta - K_2A$ versus N for experimental data taken at a fixed load and stroke should be a straight line of slope F_6 passing through origin. Such a plot is shown in Figure 4 with lines corresponding to three different values of F_6.

In obtaining the data for any one line the solenoid is operated at full voltage with a certain load and stroke. For each of several values of A, tests are run at different cycling speeds until steady temperatures are reached. Then with K_2 definitely known from the continuous heat run, and K_1 tentatively assumed equal to zero, y may be worked out and plotted against N.

If zero is the correct value of K_1, the data for different values of A will all have the same slope; if $K_1 = 0$ is too low, the points for the lesser values of A will describe a line of slightly lower slope than the points for the higher values. At this juncture, various values of K_1 should be tried (usually less than 0.25) until divergence in the data is eliminated. Once determined, this same value of K_1 should cause data taken at other strokes and loads to correlate equally satisfactorily.

Tests made at convenient fractions of full load are plotted in the manner of Figure 4. Thus F_6 becomes known, as a function of stroke, for each of several loads. Figure 5 shows the final arrangement of the data for an all-weight load.

Adaptation to Mixed Weight and Spring Loads

With mixed weight and spring loads, the F_6 values will be less than those of Figure 4. To determine them accurately, it would be necessary to run a complete set of tests for each of several spring-weight combinations, but a good approximation can be deduced from the tests with all-weight loads as follows: Equation 4 shows the closing time to be proportional to $\sqrt{W}$ for a given total load and stroke. This, in turn, makes H_c and H_i

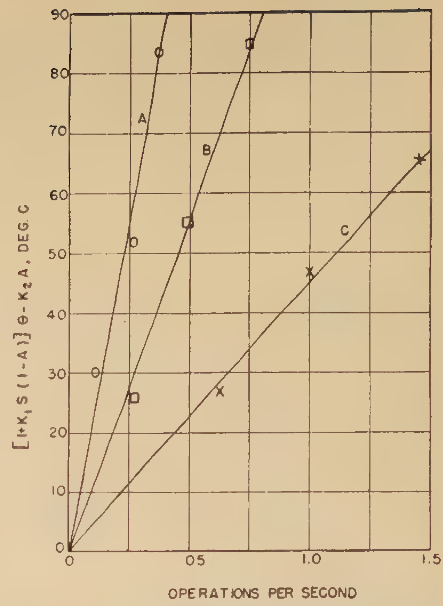

Figure 4. Data plot for determining the load-stroke function, F_6, of an a-c solenoid

A—100 per cent stroke
B— 70 per cent stroke
C— 40 per cent stroke
100 per cent rated load

vary substantially as $\sqrt{W}$. Following this through equations 10 and 11 to 15, it will be seen that F_6 is also proportional to $\sqrt{W}$. Thus for a mixed load, the F_6 for an all-weight load of the same magnitude should be multiplied by $\sqrt{W/L}$ to obtain a proper value for use in equation 15.

Advantages of the Method

It has been shown that K_2 is determined from a single preliminary heat run and

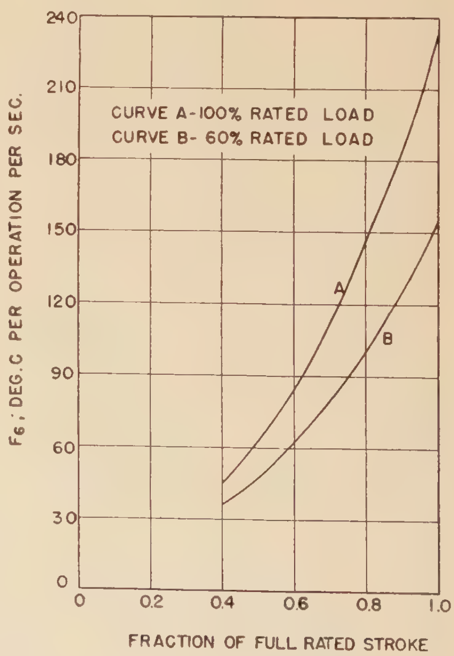

Figure 5. Load-stroke function, F_6, of an a-c solenoid with a purely weight load

that a proper K_1 may be found from a few duty-cycle runs taken at various values of A and N while holding the load and stroke constant. Thereafter, one duty-cycle heat run will suffice to determine F_6 for any one combination of L, W, and S, since a single experimental point and the origin can locate a line in Figure 4. However, several points are desirable for each line in Figure 4 to minimize experimental error. Even so, the number of tests required to completely cover all possible combinations of L, W, S, A, and N is greatly reduced by the use of this correlation method. Furthermore, experimental errors are readily detected, and the extrapolation of the data beyond the scope of the investigation is made more certain.

Applications Other Than A-C Solenoids

D-c solenoids in which a relatively large current is allowed to flow for a brief time during the pickup period are often used to increase the work output for a given size. As the plunger is seated, a cutout switch or relay operates to insert resistance and reduce the current to a suitable continuous value. Since, as with a-c solenoids, it is difficult to compute the energy input during the closing period, the correlation method of this paper is useful in organizing the duty-cycle-heating data.

In general, the method may be applied to any electrical device subjected to regular heating cycles so long as the cycling period is substantially less than the thermal time constant of the device. It is particularly useful when there is a complex transient heating effect in each cycle.

Appendix

Symbols

x = instantaneous plunger gap, inches
s = total plunger stroke, inches
S = fraction of full rated stroke
f = instantaneous magnetic force parallel to plunger travel, pounds
L = total load opposing magnetic force, pounds
W = equivalent weight of moving parts, pounds*
v = velocity of plunger, inches per second
g = acceleration of gravity, inches per second squared
E = kinetic energy, inch-pounds
t = time from start of plunger motion, seconds
T = total plunger motion time, seconds
P = power input, watts
H = energy input during closing period, watt-seconds
A = fraction of time solenoid is energized
N = operations per second
R = heat flow resistance, degrees C per watt
θ = coil temperature rise above ambient, degrees C
K, k denote constants.
F denotes a function, the independent variables of which are given in the parentheses following the symbol.
Bars above quantities indicate average values.
Prime superscripts on R's refer to thermal resistances existing when the plunger is seated.

SUBSCRIPTS

x refers to instantaneous position of plunger.
s refers to sealed position of plunger.
c refers to coil.
i refers to iron.
ca refers to coil to ambient air.
ci refers to coil to iron.
ia refers to iron to ambient air.

* An element of mass $\Delta M'$ in an associated mechanism may be represented by an equivalent mass ΔM attached directly to the plunger if

$$\Delta M = \Delta M'\left(\frac{v'}{v}\right)^2$$

where v' is the velocity of $\Delta M'$ when the plunger velocity is v.

A Simple Method for the Determination of Bushing-Current-Transformer Characteristics

S. D. MORETON
ASSOCIATE AIEE

Synopsis: Now that excitation characteristics are being made available to the application engineer, it has become increasingly important that a simple accurate calculation method be used to determine the errors. "Inphase addition" has all the advantages of simplicity, and, under careful analysis, it is apparent that over the normal ranges involved, the more complicated procedure of vector addition is not warranted. This simple method has been extended to cover complete curve drawing from only one calculation by a graphical process.

In the past, characteristics of bushing current transformers have been presented in the form of curves of true ratio versus primary current for a variety of different burdens and power factors. Such curves were obtained by tests on the actual transformers and were the only data available to the application engineer. The art of calculating current-transformer characteristics from excitation curve data has been known for some time;[1,2] only recently, however, has popular attention been focused upon it as a method[3,4] for determining the transformer errors of a particular application. With this in mind, the American Standards Association standards have been written to foster a calculation method,[5] although no specific method was decided upon.

It has been realized that such a radical change in application procedure might well result in difficulties unless the method was reasonably easy to use, and it has, therefore, been suggested that the errors introduced by the relatively simple method of inphase addition be investigated more fully. The conclusion was reached that this method was the practical answer. These errors are best shown graphically, as in Figure 1, which gives a comparison of calculated results for three of the ratios of a 600/5 multiratio bushing current transformer with a two-ohm burden at 90 per cent power factor and 50 per cent power factor, as well as inphase addition. It is immediately apparent that the errors introduced by disregarding the angular difference between the secondary current and the exciting current are small for the normal range of relay burden power factors.

Conclusion

When all the variable factors are taken into consideration, as discussed under "Accuracy Factors," it is evident that the errors introduced by inphase addition are a relatively small part of the total pos-

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S. D. MORETON is engineer in the relay section of the General Electric Company, Philadelphia, Pa

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sible variation, and thus, from the standpoint of the application engineer, inphase addition is justifiable for the normal range of burden power factors. This is particularly evident by a comparison of Figures 1 and 8.

Calculating Theory

Basically, the relationship between secondary ampere turns, exciting ampere turns, and primary ampere turns is a vectorial one, where the calculations are based on the equivalent circuit of Figure 2. The most time-consuming part of the calculations is the vector addition, whether it is performed mathematically, graphically, or by vector charts. By considering this vector relationship as a scalar relationship much of the work is avoided.

Calculating Procedure for Determining Ratio Correction Factor

The calculation procedure for inphase addition is based on the equivalent circuit of Figure 3, with steps and formulas as follows:

1. Compute the necessary secondary voltage to force the desired secondary current through the total burden, as: $E=IZ$.

2. Find the secondary exciting current required to produce this secondary voltage from the excitation curve, see Figure 4.

3. Add this secondary exciting current arithmetically to the secondary current and multiply by the number of secondary turns to obtain the primary current, as: $I_p=N(I_e+I_s)$.

The ratio correction factor (RCF) may be defined as the ratio of primary ampere turns to secondary ampere turns. In a bushing current transformer, the primary winding has one turn, therefore:

$$RCF=\frac{I_p}{NI_s} \tag{4}$$

$$RCF=\frac{N(I_e+I_s)}{NI_s}=1+I_e/I_s \tag{5}$$

and this may be found without passing through step 3.

Calculating Procedure for Determining Phase Angle β

In those applications that involve the comparison of two currents or a current and a voltage, it might be desirable to determine the phase-angle error of the current transformer. It may be found by an extension of the method of inphase addition and by the use of the phase-angle chart, Figure 5. Excitation characteristics are required that give not only the total exciting current but also the lagging excitation phase angle between the exciting current and the secondary voltage. Figure 6 is typical of this type data and is presented on a per-turn basis.

The chart is entered at a per-unit exciting current, I_e/I_s, of equation 5, which could be rewritten as:

$$RCF-1=I_e/I_s \tag{6}$$

The phase-angle error is read off directly, using the curve that corresponds to the "difference angle," equal to the burden phase angle minus the excitation phase angle ($\theta_s-\alpha_e$). The phase-angle error is positive when the difference angle is positive.

Accuracy Factors

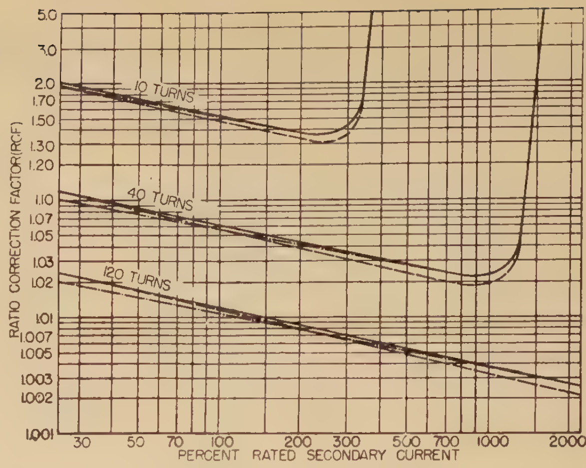
Factors which influence the accuracy of the calculated results are variations of excitation characteristics of the current transformer from the typical curve values and variations in the magnitude of the burden.

The excitation requirements of electrical iron are not only a function of the size and shape of the core but also a function of the amount and kind of impurities, the type of rolling process used, the annealing temperature and subsequent cooling, as well as the previous magnetic history. Some of these factors are very difficult to control, with the result that the final characteristic, as published by the manufacturer in the form of a typical excitation curve, is, of necessity, only an average curve. All of the factors lumped together and controlled with the usual manufacturing tolerances can well result in a variation as great as plus or minus 50 per cent in the total exciting current at any one particular flux density for various lots of iron. By more rigid control and selective choice of the iron by sampling processes, this can be reduced to plus or minus 25 per cent; see Figure 7 for a typical band curve. Even this variation would seem to be intolerable, were it not for the fact that this represents a variation of the ratio error of the transformer and not of the true ratio. In the majority of applications, where the maximum desirable RCF is less than 1.1, and where the transformer is operating below the knee of the excitation curve, the maximum possible variation will be plus or minus 25 per cent of this ten per cent error, or plus or minus 2.5 per cent in the RCF; see Figure 8 for a typical band curve.

The use of a calculating method for the determination of bushing-current-transformer characteristics requires a knowledge of the magnitude of the burden which, in most instances, consists of relays that have iron cores, and thus have

Figure 1. Calculated ratio characteristics; two ohm burden

——— Inphase addition
— · — 90 per cent power factor
— — — 50 per cent power factor



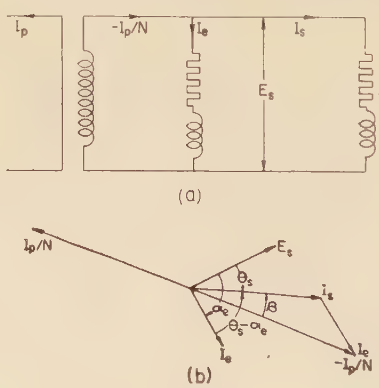


Figure 2

(a). Exact equivalent circuit
(b). Vector diagram of (a)

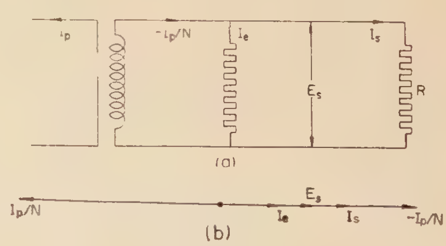


Figure 3

(a). Approximate equivalent circuit
(b). Scalar diagram of (a)

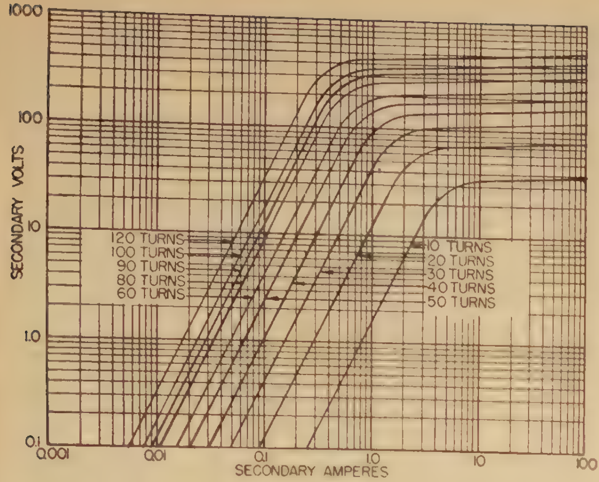


Figure 4 (left). Secondary excitation characteristic, 600/5 multiratio bushing current transformer

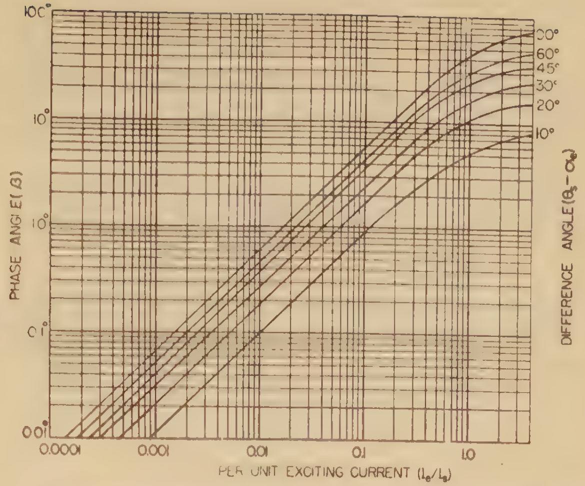


Figure 5 (right). Phase-angle error chart

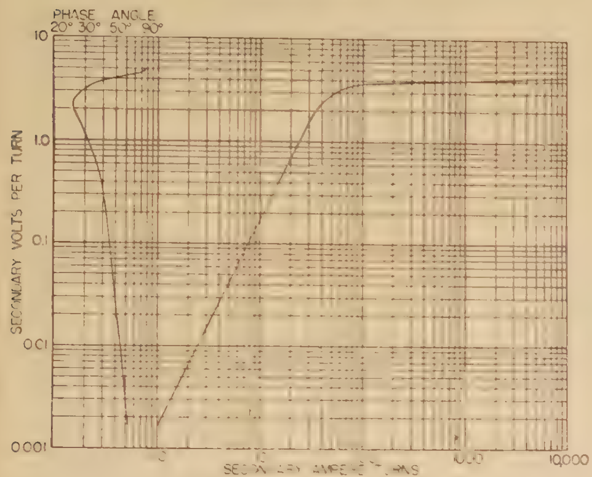


Figure 6 (left). Excitation characteristic

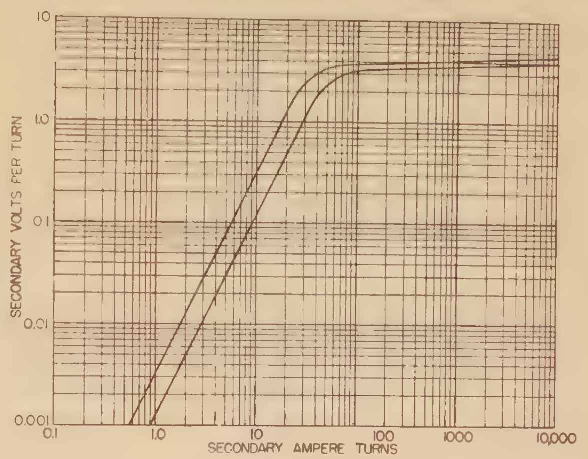


Figure 7 (right). Band-excitation characteristic

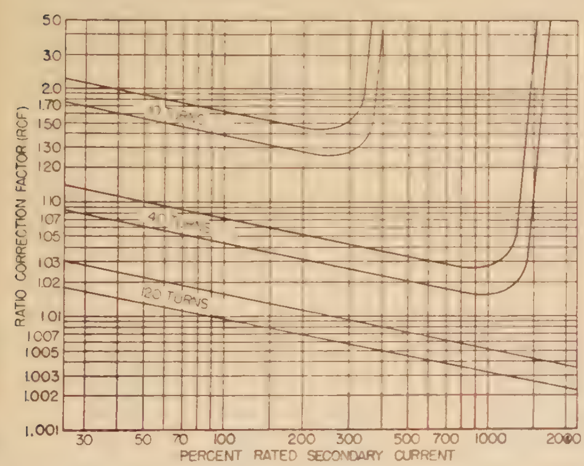


Figure 8 (left). Band-ratio characteristic on three ratios of a 600/5 multiratio transformer

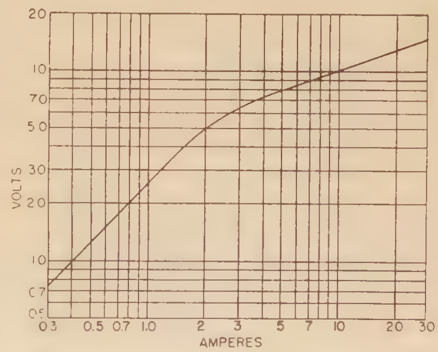


Figure 9 (right). Burden characteristic of time overcurrent relay on 1.5 ampere tap

inherently nonlinear impedances. Fortunately, the majority of relays have magnetic circuits which contain series air gaps so that the main part of the exciting current is required to establish the air-gap flux, and thus, large variations in exciting current of the iron core present only small variations in burden between units. Therefore, relays have relatively constant burden characteristics up to the point of iron saturation. Beyond this point, the reactance component of the burden is inversely proportional, approximately, to the current so that the current transformer will hold up to a higher value of primary current if burden saturation occurs before current transformer saturation. Typical burden values of relays are published by the manufacturer and are usually measured at the pickup current of the relay. If they are of the type that depends upon saturation to obtain its characteristics, such as a time overcurrent relay, the application engineer would require burden characteristics, such as shown in Figure 9, to make a thorough analysis. Thus, it seems inevitable that as the art of calculation becomes more widespread, the application engineer will require more complete information; see Figure 10 for a calculated *RCF* curve, based on composite characteristics of the overcurrent relay of Figure 9 and the transformer shown in Figure 4.

Graphical Method

When the ratio errors are desired for a range of primary current values for a multiratio transformer, the labor at calculation becomes tremendous, even with the simplification of inphase addition. To reduce this work, a graphical method has been developed, which eliminates the point-by-point method of curve drawing and permits a complete characteristic to be drawn from one calculation by the use of a master template, Figure 11.

Note that the shape of this template is a function of the type of iron and the size of the log-log co-ordinate paper only.

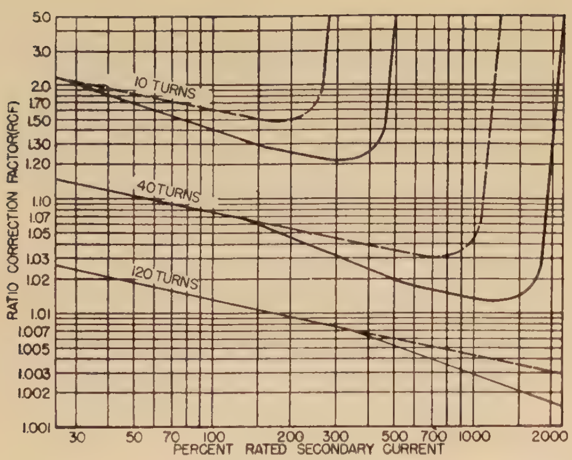


Figure 10. Calculated ratio characteristic, 600/5 multiratio bushing current transformer

— — — 2.5 ohm burden
———— time overcurrent relay burden on 1.5 ampere tap (2.5 ohms at pickup)

The derivation of the method and the development of the shape of the template are given in the appendix.

The co-ordinate paper, Figure 12, has scales of *RCF* versus per cent secondary current (dotted lines) and per cent primary current (solid lines). Thus, one is able to find the *RCF* for a given primary current, the *RCF* for a given secondary current, or the secondary current in terms of the primary current directly. Heretofore, when it was desired to determine the secondary current from a known primary current, it was necessary to make several calculations, using the ratio curves until formula 4 was satisfied.

Graphical Procedure for Determining RCF

The calculation procedure for this graphical method is:

1. Use formulas 7 and 8 to determine the per cent secondary current and the *RCF* that corresponds to the reference point *P* of the template for the particular burden and tap of the transformer involved.

2. Locate this point on the co-ordinate paper.

3. Set the template with its reference point on the corresponding point of the co-ordinate paper and line it up horizontally.

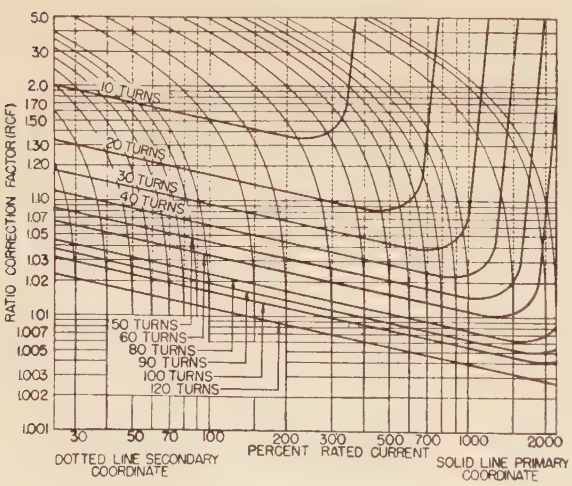


Figure 12. Calculated ratio characteristic, 600/5 multiratio bushing current transformer

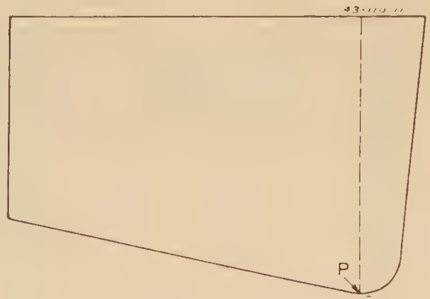


Figure 11. RCF template

4. Draw the complete characteristic curve, using the template as a guide.

The formulas required to determine this guide point of maximum permeability, *P*, are:

$$\%I_s = C(N/Z) \tag{7}$$

$$RCF = 1 + D/(N\%I_s) \tag{8}$$

where *C* and *D* are constants of the particular transformer involved and are functions of the shape and type of iron in the core, as well as the frequency. The constants *C* and *D* may be determined directly from the excitation curve.

Graphical Procedure for Determining Phase-Angle Errors

Where it is desirable to determine the phase-angle error of the current transformer, it may be obtained as a part of the graphical process by the use of Figure 5 entering at a per-unit exciting current equal to $RCF-1$ of Figure 12 and at a difference angle equal to the total burden phase angle minus the exciting current phase angle, as obtained from the template of Figure 13 when located on the co-ordinate paper according to step 3.

Summary

1. Ratio errors of bushing current transformers may be calculated by inphase addition with acceptable accuracy.

2. Phase-angle errors may be calculated with little extra effort.

3. A complete analysis of a relay application requires a knowledge of the relay impedance for all values of current.

4. Ratio-characteristic curves may be drawn with a minimum amount of labor by the use of a template method.

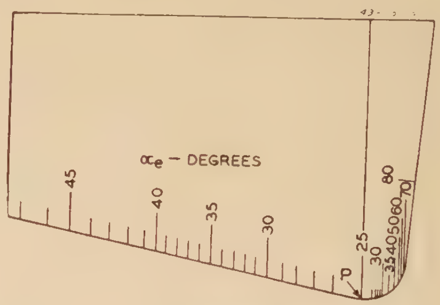


Figure 13. RCF template with excitation phase angle

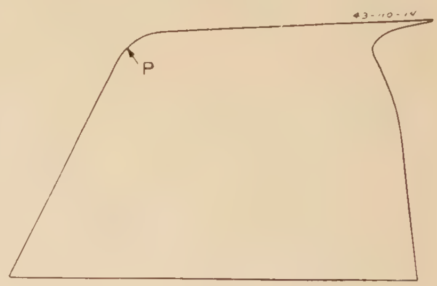


Figure 14. Excitation template

5. The use of special co-ordinate paper presents an ideal way to give ratio characteristics.

Appendix

Graphical Theory

Excitation Curve

Fundamentally the theory is based on the fact that the a-c excitation characteristics of electrical iron may be expressed mathematically as:

$$NI_e/l = f_1(E/NA, f) = f_1\left(\frac{I_sZ}{NA}, f\right) \tag{9}$$

or

$$NI_e/l = f_2\left(\frac{E}{NA}\right) = f_2\left(\frac{I_sZ}{NA}\right) \tag{10}$$

for any one particular frequency. This equation is in the form of:

$$P_y = f(Q_x) \tag{11}$$

and hence, if plotted on log-log paper with the exciting current as abscissa and the secondary voltage as ordinate, it represents a family of curves all of which have the same shape. Consequently one template, Figure 14, whose shape is determined solely by the magnetic properties of the iron and the co-ordinate paper can be used as a guide for drawing all curves of the family if one reference point, P, is marked on the template and is specified for each curve. It is convenient to select this reference point at an excitation corresponding to maximum permeability of the iron, which is the point on the excitation characteristic that is tangent to a 45-degree line and has an ordinate value of J and an abscissa value of K. The co-ordinates of the reference point on the log-log paper are:

$$E = JNA \tag{12}$$

$$I_e = Kl/N \tag{13}$$

The constants J and K are functions of the primary-current frequency and the type of iron in the core only.

If the template is set with its reference point on the co-ordinate point and it is lined up with the axis, curves for any value of secondary turns may be drawn with the template as a guide. The ordinate of the template is proportional to the secondary voltage and from an inspection of equation 10 it is apparent that it is also proportional to the flux density since

$$E/NA = 4.44 B_{\max.} f \times 10^{-8} \tag{14}$$

RCF Curves

Formula 10 may be expressed as:

$$\left(\frac{NI_e}{l}\right)\frac{NA}{ZI_s} = \frac{NA}{ZI_s} f_2\left(\frac{ZI_s}{NA}\right) \tag{15}$$

or

$$\frac{N^2A}{lZ}\frac{I_e}{I_s} = f_3\left(\frac{ZI_s}{NA}\right) \tag{16}$$

Substituting for I_e/I_s from equation 6 the following is obtained:

$$\frac{N^2A}{lZ}(RCF-1) = f_3\left(\frac{ZI_s}{NA}\right) = f_3\left(\frac{E}{NA}\right) \tag{17}$$

Equation 17 is also of the form of equation 11 and consequently represents a family of curves, when drawn on log-log paper, having scales of $(RCF-1)$ versus I_s, all of which can be drawn using a single template as a guide by setting the reference point, P, of the template, Figure 11, on the co-ordinates given by equations 12 and 13. It will be found more convenient to use equation 12 combined with equation 1 as:

$$I_s = \frac{JNA}{Z} \tag{18}$$

and equation 13 combined with equation 6 as:

$$RCF - 1 = \frac{I_e}{I_s} = \frac{Kl}{NI_s} \tag{19}$$

Equations 18 and 19 can be converted to read in terms of per cent secondary current, $\%I_s$ (based on five amperes as 100 per cent) as:

$$\%I_s = \frac{20JNA}{Z} = C\frac{N}{Z} \tag{20}$$

$$RCF - 1 = \frac{20Kl}{N\%I_s} = D/N\%I_s \tag{21}$$

where

$$C = 20JA \tag{22}$$

$$D = 20Kl \tag{23}$$

Here again it should be noted that the shape of the template of equation 17 is a function solely of the magnetic properties of the iron and the co-ordinate paper.

To convert this curve of $(RCF-1)$ versus per cent secondary current, the dotted co-ordinate lines of Figure 12, to $(RCF-1)$ versus per cent primary current it has been found most convenient to operate on the per cent secondary current scale itself by equation 6, where I_e/I_s represents the ratio error in per-unit values. Thus every co-ordinate value of $(RCF-1)$ and per cent secondary current has a corresponding co-ordinate value of $(RCF-1)$ and per cent primary current according to the equation:

$$\%I_p = 100(RCF-1) + \%I_s \tag{24}$$

which gives the resulting per cent primary current scale of solid co-ordinate lines in Figure 12.

For convenience in reading the numbers of the $(RCF-1)$ or ordinate scale they may be written as $(RCF-1)+1$ or RCF without changing either the shape of the template or the shape of the co-ordinates since equation 4 can be expressed as:

$$RCF = \%I_p/\%I_s \tag{25}$$

which is equivalent to equation 24.

Phase-Angle Curves

The abscissa of the RCF template is proportional to secondary voltage by equation 17, it is therefore proportional to the flux density by equation 14, and thus it is also proportional to the excitation phase angle of the iron, see Figure 6. Therefore the RCF template may be marked off in degrees of phase angle of the exciting current as in Figure 13, where the markings on the template are a function of the type of iron and the frequency only. The phase-angle error can be determined most easily by the use of a simplification of the vector charts following the procedure outlined earlier.

Glossary

E—Induced secondary voltage.
N—Number of secondary turns.
A—Cross-sectional area of magnetic circuit.
l—Mean length of magnetic circuit.
I_s—Secondary current.
$\%I_s$—Secondary current in per cent based on five amperes ($\%I_s = 20I_s$).
Z—Total secondary burden.
I_e—Excitation current based on secondary.
I_p—Primary current.
$\%I_p$—Primary current in per cent based on tap and five amperes in secondary circuit.
θ_s—Phase angle of total secondary burden Z.
α_e—Phase angle of exciting current.
β—Phase angle error of current transformer.
f—Frequency of supply in cycles per second.
$B_{\max.}$—Crest flux density in the core.

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Report on Application of Lightning Protective Devices in Wartime

AIEE LIGHTNING ARRESTER SUBCOMMITTEE

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication and other guides and reports in this series have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present standards have been prepared.

I. Purpose and Scope

THE object of this report is to show how critical materials can be conserved in the application and use of lightning arresters to meet the wartime requirements of our country. This objective may be accomplished in two ways:

1. By making greatest use of protective devices. This refers to rebuilding of old arresters and applying new arresters close to their maximum capability, thereby reducing the content of critical materials in the arresters themselves.

2. By proper use and maintenance of protective devices, failures from lightning of important machines and equipment which would require large amounts of critical materials and man-hours to replace can be minimized, thereby maintaining a high degree of service to important war loads.

It is not to be implied that the suggestions contained in this report are the only means worth-while in attempting to save critical materials, nor that they are recommended as expedient in all cases. It should be clearly understood that local conditions, service requirements, and many other such factors may have to be considered before reaching a decision as to whether or not it is advisable to use one or more of the proposed schemes.

The importance of a check up and study on existing systems of overvoltage protection against lightning to obtain better protection cannot be overemphasized. This type of study applies to both new and existing installations.

II. Suggestions for Saving Critical Material

The suggestions presented and discussed hereinafter are backed in most cases by successful operating experience. The experience record, however, does not pretend to be all inclusive as it has not been expedient to survey thoroughly the entire field throughout the country to obtain and analyze detailed records. Where diverse views of committee members have existed, this situation is pointed out.

1. Applying arresters on basis of voltage rating.
2. Rebuilding and revamping old arresters.
3. Short connections between arresters and equipment.
4. Line-type arresters in place of station-type.
5. Protection of equipment not already protected.
6. Protective devices other than arresters.
7. Shielding of stations.
8. Tests and maintenance of arresters.

The eight suggestions are discussed in order.

1. Applying Arresters on Basis of Voltage Rating

It is generally recognized that a lightning arrester has a maximum normal frequency (60-cycle) voltage rating just as any other piece of equipment like a transformer, breaker, or disconnecting switch. Although tolerances of overvoltage for these latter types of equipment are specified in standards, they are undoubtedly often exceeded in service. The arrester differs from other types of apparatus in one respect, namely, that its maximum voltage rating should not be exceeded under any operating condition; otherwise, the arrester may be severely damaged. The use of an arrester on a system where the line-to-ground voltage exceeds the arrester rating, even for a few cycles, may well be viewed with concern. If arrester failure occurs because of abnormal system overvoltage, it is very likely that the arrester has been misapplied.

In general, arresters listed in the manufacturers' catalogs for nongrounded neutral systems have a maximum rating of approximately five per cent above line-to-line system voltage and those applied to effectively grounded neutral systems have a factor of safety of approximately 40 per cent above system leg voltage (80 per cent of the line-to-line voltage).

In cases where the ungrounded neutral arrester has been used on a grounded neutral system, a saving may be effected by using the grounded neutral arrester, provided the system neutral is grounded effectively. Also, there are certain cases where an accurate calculation of the system overvoltage on grounded neutral systems has resulted in application of arresters having a lower rating than the general manufacturer's guide for the selection of arresters indicates.

For example, on one particular system rated 132-kv grounded neutral, an arrester having a maximum line-to-ground rating of 109 kv has been used successfully. In this particular case the use of a lower arrester rating has resulted in the use of power equipment with lower insulation (115-kv class insulation instead of 138-kv class which would normally have been required). In this connection, it should be realized that the lower the rating of arrester used, the greater the degree of lightning protection to the equipment, but on the other hand, there is less factor of safety in the arrester.

2. Rebuilding and Revamping Old Arresters

Another factor to be considered is the savings of materials possible by rebuilding or revamping old type arresters. Several companies have reported that they have been doing work along these lines for some time. In the case of oxide-film arresters which were extensively used in the past, it is general practice in a number of companies to reduce the number of

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Personnel of lightning arrester subcommittee: I. W. Gross, *chairman;* Edward Beck, F. M. Defandorf, R. H. Earle, H. N. Ekvall, Herman Halperin, J. R. McFarlin, E. E. Piepho, W. J. Rudge, A. H. Schirmer, H. R. Stewart, J. M. Towner, E. R. Whitehead, E. H. Yonkers.

This report was prepared by the AIEE lightning arrester subcommittee of the committee on protective devices for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been formally approved by the standards committee or the board of directors.

cells in existing arresters which, in effect, means reducing the arrester rating to match more nearly anticipated normal frequency overvoltage conditions of the circuit where they are used. The principle involved has been discussed. Likewise, the changing of four-leg oxide-film arresters to the three-leg type has been an accepted practice for some time with outstanding success from the viewpoint of added protection and absence of arrester failure. Of course, in such cases the 60-cycle maximum rating of the remaining three legs must be checked against the system overvoltage under fault conditions. In some cases where the three-leg 60-cycle rating was too low, part of the fourth leg was left in service to supply the deficiency.

Successful rebuilding of mica-spaced and open-gap auto-valve arresters has also been reported. The rebuilding of old arresters supplies added protection to equipment where they are installed by lowering the protected level and also may make available some of the material removed for other arrester locations or spare parts. The rebuilding of even obsolete arresters may in some cases be warranted, if new arresters are not available. The added protection of rebuilding obsolete arresters may in some cases be justified, although the protected level may be higher than would ordinarily be desired.

3. Short Connections Between Arresters and Equipment

The desirability of having short connections between the arrester and the equipment it is to protect has been discussed extensively in technical literature in the recent past. It probably does not require further amplification here. It is well known by theory and test that while the arrester may hold a given voltage at its own terminals, this voltage may be quite different at the terminals of protected equipment if long leads intervene to the lightning arrester. Even in distances of 25 to 50 feet, on steep front surges, it is generally recognized that the voltage at the equipment may be increased appreciably by virtue of the lead length.

The importance of considering this factor is evident for two reasons: first, less protection is supplied to equipment already in service where lead length is considerable. This is, of course, equally true of new installations of protective equipment. Second, there may be some saved or salvaged copper by proper consideration of this lead-length feature.

Another saving which can be effected in copper conductor is that of using substantial structural work in stations such as columns, trusses, and so forth, for part of the lightning discharge path to ground. In many cases in the past, common practice has been to extend separate grounding leads from the ground end of the arrester to ground, even though the main structural part of the station was electrically tied into the grounding system. This extra refinement, it is felt, under wartime emergency conditions, is not necessary and probably results in very little, if any, increased protection by adding the ground lead from the arrester to the equipment where a suitable and substantial steel structure or network already exists.

4. Line-Type Arresters in Place of Station-Type

In the installation of new equipment, it often is possible to maintain sufficient margin between the distribution-type-arrester characteristic and the insulation strength of present-day equipment. Where old equipment only is involved, it is doubtful whether the substitution is worth while, and in fact, it may often be found that the protective level supplied by even modern station arresters is unduly high so that the use of line-type arresters cannot even be considered.

Several successful applications of line-type arresters on new equipment have been made with satisfactory operating experience. It should be pointed out, however, that many of the applications coming under this classification result more often in economies than in a material reduction in the use of critical material or in saving of manpower.

5. Protection of Equipment Not Already Protected

Secondary savings considerably greater than cost of applying lightning arrester protection are usually possible in cases where equipment is not now protected. It is not the intention of the committee to recommend that all equipment not now protected should be immediately supplied with arrester protection, but there are undoubtedly existing installations and new ones being made where the expenditure of a small amount of critical material in lightning arresters may produce large secondary savings by preventing failures of important equipment. This feature is pointed out as one worth considering and studying in individual applications. The decision reached will be influenced by many factors such as exposure of equipment to lightning, its importance in service, the possibility of replacement, cost and time of replacement, and similar factors.

6. Protective Devices Other Than Arresters

While the lightning arrester has been mentioned frequently in this report, the general features of application of overvoltage protective devices considered here apply in most cases to other protective equipment such as rod gaps, shielded gaps, De-ion gaps, and similar devices. Where to use one in preference to another is entirely outside the scope of this report to recommend. However, it is suggested that careful consideration be given in the application of any protective devices to insure that a suitable margin between the voltage level held and the strength of the equipment be maintained. It should be pointed out, however, that all of these devices mentioned have been reported by some users as giving successful and satisfactory service within their scope of application.

7. Shielding of Stations

Where a large amount of equipment is placed in one location such as at major stations, the general practice in recent years has been to supply adequate shielding from lightning by the use of elevated ground wires or ground networks, and even in some cases, by individual lightning rods shielding equipment. While such protection requires the use of some critical material, it may well be considered from the point of view of spending a relatively small amount of critical material for shielding to prevent damage to equipment having a much larger amount, the replacement of which would probably necessitate an expenditure of critical material and manpower.

8. Tests and Maintenance of Arresters

In so far as the arrester itself is concerned, it is suggested that special consideration be given to testing arresters wherever possible and feasible, to insure that they are in the best of condition to perform their expected functions. Several years ago recommendations were made by the AIEE lighning arrester subcommittee on methods of testing some types of arresters. Since that time, a large number of arresters have been tested, and other methods of test or refinements have been

Interim Report on Emergency Overloads on Overhead Conductors

AIEE WORKING GROUP ON TOWERS, POLES, AND CONDUCTORS

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication, and other guides and reports in this series, have been prepared for the information of users during the war emergency. Upon termination of the war emergency they will be reconsidered by the standards committee and the committees which prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment without changing the fundamental basis on which the present standards have been prepared.

PRESENT restrictions in the use of materials and sharp increases in loads may make it necessary to load existing lines to the thermal capacity of the conductors. The purpose of this report is to call attention to the limiting factors and the information available from which to evaluate the limits.

The problem divides itself into two parts, the temperature at which the conductor may be operated without impairing its strength or destroying its covering, and the temperature the conductor will attain for a given current density under any given conditions. Neither of these can be calculated with any degree of precision, and the probable error must be duly considered in establishing emergency ratings for any conductors.

I. Permissible Temperatures

A. Loss of Strength Caused By Annealing

The annealing point of the softer metals hardened by cold working, is not very definite. Variations are introduced by impurities and by the amount and rate of cold working. Time is also an important factor, and it is not definitely known if effects of intermittent heating are directly additive or not.

Aluminum. G. W. Stickley gives the following figures for annealing of hard drawn aluminum. "In tests of wire heated one hour at 100 degrees centigrade the strength was decreased only two per cent, and after heating at this temperature for as long as four years the decrease was only 15 per cent. In similar tests of wire heated one hour at 150 degrees centigrade the strength was decreased only about five per cent. It should be noted that although overloads might have a small effect in decreasing the strength of aluminum conductors, they would have still less effect upon the strength of steel reinforced aluminum cable. Tests of the high-strength steel wire used as the core of ACSR have shown that heating at temperatures as high as 260 degrees centigrade for two months actually increases the strength of the wire slightly."

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Personnel of the working group on towers, poles, and conductors: Edwin Hansson, *chairman;* C. A. Booker, H. A. Clark, A. E. Davison, F. W. Packer, G. W. Stickley, E. O. Wahlstrom.

This interim report was prepared by the AIEE working group on towers, poles, and conductors of the transmission subcommittee of the committee on power transmission and distribution for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been formally approved by the standards committee nor the board of directors.

Howell and Paul[1] state that commercially pure aluminum in the hard drawn temper, designated 2*S*-*H*, is not materially softened by prolonged exposure to temperatures in the region of 200 degrees Fahrenheit.

Copper. Many data on annealing of copper have been published, but the values given vary over an extreme range. If the conductor is kept at 171 degrees centigrade, Anaconda claims ten per cent softening in one hour, while General Cable Company finds that it requires 6.7 hours to produce ten per cent softening at 175 degrees centigrade. H. R. Stewart states results from actual tests as follows: A number two copper wire held at 215 degrees centigrade and a number 1/0 held at 160 degrees centigrade for four hours each lost about five per cent of its strength. Kidder and Woodward[3] find that copper may be held at 180 degrees centigrade for eight hours on six consecutive days (total 44 hours) with 16 per cent loss in strength, and when heated to 200 degrees centigrade for the same length of time it lost 35 per cent. From this they conclude that the danger zone lies about 175 degrees centigrade for short duration.

A very complete investigation is presented by Myron Zucker.[4] It shows that the annealing temperature is affected by the amount of cold working and the purity of the copper. The harder the copper, the lower the annealing temperature. A small percentage of silver will raise the annealing temperature considerably, while oxygen will lower it. He summarized his findings in curve form, giving a spread of from 40 to 1,200 hours at 100 degrees centigrade for copper in the electrolytic range. He also states that an X-ray diffraction test can be used to determine the annealing characteristics of copper without destroying the sample. The beginning of recrystallization indicates a loss of five per cent of the tensile strength of the wire.

In most cases, annealing tests on copper

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used. It cannot be too strongly urged that a special attempt should be made to keep the arresters and their grounding connections in the best possible condition.

Summary

The features in the preceding paragraphs have been presented and discussed with the thought in mind that the information may be helpful to those who have the lightning or overvoltage protection problem to deal with. It is not intended that all of these suggestions be applied without careful study and thought; in fact, unless such a detailed study is made of systems now in use or proposed, the full benefits of the applications suggested will most likely not be obtained. It is hoped that this paper will stimulate constructive thought on the part of those who have the responsibility of designing and operating protective systems already installed or to be installed in the future.

It should be kept in mind that in applying some of these *suggestions*, the failure rate on the protective devices themselves may increase, but it is believed that if the conditions of each situation are considered and action taken on an engineering basis, the net over-all effect will be of considerable benefit to the war effort.

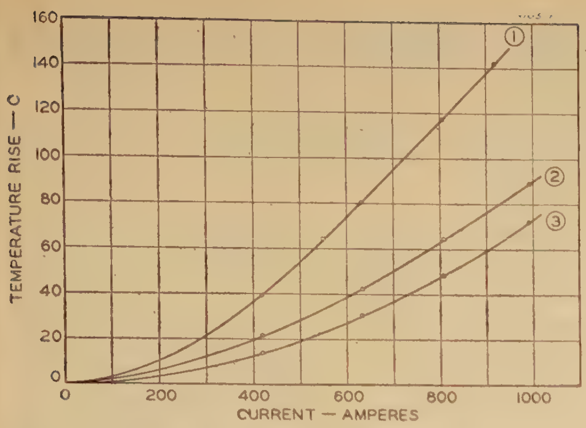

Figure 1. Temperature rise of 795,000-circular mil steel-reinforced aluminum cable, new

Tests made at 60 cycles indoors in still air on unused cable from stock. Cable not weathered but no longer bright

1. Cable in Ohio Brass number 77,898 suspension clamp (without armor rods)
2. Cable with armor rods in Ohio Brass number 77,613 suspension clamp
3. Bare conductor, with neither armor rods nor suspension clamp

have been designed to determine the length of time required for complete annealing at different temperatures. The difficulty in exterpolating these data to a time–temperature combination which would give a small percentage of annealing may be responsible for the wide discrepancy in values.

Before the question of annealing can be definitely answered, information on the effect of the following items must be known:

Various impurities commonly found in commercial metals.

The cold working required to make the metal into wires or cables.

The stresses to which the conductor normally is subjected.

Intermittent heating and possible recovery between heating periods.

Copperweld. The strength of a copperweld conductor is predominated by the steel content, and it is not likely to be loaded to a point where its mechanical strength is impaired. Rolf Selquist suggested that the current-carrying capacity of copperweld be limited to 70 per cent of that of a copper conductor of the same cross section.

B. Protection of Wire Coverings

Kidder and Woodward[3] state that with a copper temperature of 175 degrees centigrade the outside surface of the covering will be 130 degrees centigrade, which is well below the impregnating temperature, and 80 degrees centigrade, or just below the standard acceptance specification with a copper temperature of 100 degrees centigrade. Myron Zucker[4] stated that recent tests on *URC* covering indicate that it can be safely operated for ten hours at 100 degrees centigrade. The compound used in older types of weatherproofing gets soft and tacky at temperatures as low as 60 degrees centigrade and the *URC* covering begins to soften at 85 degrees centigrade. It is reasonable to assume that such temperatures are going to accelerate the deterioration of the covering. In many cases it may well be that this would be of little importance as compared to the additional capacity gained.

C. Preservation of Clearances

Operating conductors at elevated temperatures will mean considerable increases in sag. Where maintenance of clearance

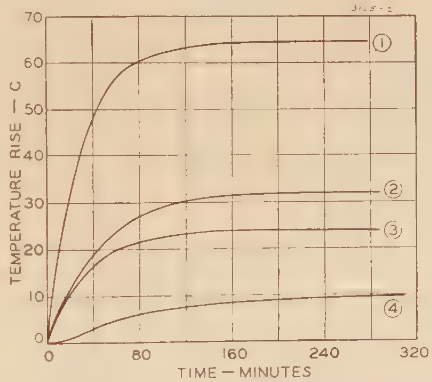

Figure 2. Rate of temperature rise of 795,000-circular mil steel-reinforced aluminum cable, new

Tests made at 60 cycles at 550 amperes, indoors in still air on unused cable from stock. Cable not weathered but no longer bright

1. Cable in Ohio Brass number 77,898 suspension clamp (without armor rods)
2. Cable with armor rods, in Ohio Brass number 77,613 suspension clamp
3. Bare conductor, with neither armor rods nor suspension clamp
4. Cable with armor rods, but without suspension clamp

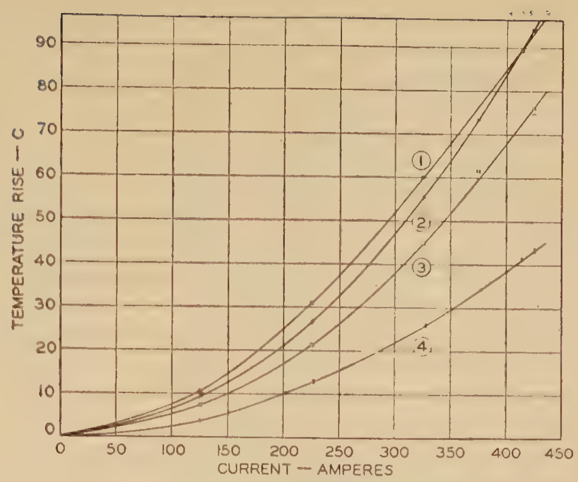

Figure 3. Temperature rise of badly oxidized seven-strand 2/0 copper cable

Tests made at 60 cycles indoors in still air. Conductor badly oxidized from many years of service

1. Cable in Ohio Brass number 70,757 suspension clamp and number 70,759 keeper (horns removed)
2. Thermocouple between core strand and outside layer
3. Thermocouple on surface of cable
4. Memco number 64 twisted sleeve

is important, these increases should be carefully checked. Even small amounts of annealing may result in permanent stretch of the conductors after a heavy ice load, and it may become necessary to resag the line.

II. Temperatures That the Conductor May Attain

A. Temperature Rise Versus Current

Several references covering the heating of conductors from electric currents are available in the engineering literature.[3,5–9] The formulas given by Schurig and Frick seem to give consistent results. Kidder and Woodward[3] give a detailed description of their method of its application.

B. Weather Conditions

The temperature rise of the conductor must be co-ordinated with the ambient temperature, wind conditions, rainfall, and so forth. The frequency and duration of high temperatures in combination with low wind velocities may be found from local weather records. These data may be plotted in various combinations to give a picture of the probability of high temperature with little or no wind coinciding with maximum load. One reason for overloading a circuit may be the tripping of a parallel circuit during a lightning storm, making it desirable to make a study of weather conditions immediately following lightning storms.

Even if the weather bureau records do not show any coincidence of maximum temperature with minimum wind, such a possibility is by no means excluded. It is also possible that some part of the line is located so as to be practically shielded from wind. Sun absorption may be exaggerated by the presence of a building wall or other reflecting surface. All of these factors should be weighed and evaluated.

Other Limitations. Clamps and joints may be a further limiting factor. It is well known that joints deteriorate with time. The modern, drawn, compressed, or automatic joint is far superior to the old sleeve joint in this respect, but deterioration in some degree is bound to take place.

Most suspension and dead end clamps in use today form a more or less closed magnetic circuit around the conductor, and losses at 60 cycles are appreciable. This was brought out by W. H. Burleson. A test on a 500,000-circular mil copper conductor gave a temperature rise of the clamp 80 per cent higher than that of the conductor proper at a current of 800 amperes. The results on another test on a 795,000-circular mil ACSR, with 550 amperes, shown in Figure 1, indicate a considerable cooling effect of the armor rods, and that the temperature of the clamp (without armor rods) reached two and one-half times that of the conductor. Figure 2 shows that while the rise is rapid during the first hour, it requires almost four hours to reach a stable condition. In a weathered conductor, Figure 3, the center or core strand may reach a temperature 15 degrees centigrade in excess of that of the surface strand. This steep gradient, on the order of 5 degrees centigrade to 7 degrees centigrade, has also been found by Zeerleder and Bourgeois.[2]

Conclusions

If permanency and safety of the line are to be considered, emergency ratings must be extremely conservative when based on our present knowledge. A sample test of the conductor in question cannot be considered a reliable answer as to the annealing characteristics of the conductors along the entire line. The lot may have been purchased from a single manufacturer, on a single order, and yet may have been made from two or more different lots of metal.

This does not necessarily mean that it would be unsafe to increase the rating of a line over previous standards. In many cases a substantial increase is possible, even if the various factors are chosen on a conservative basis, and the final limitation may be found to lie in considerations other than the temperature of the conductor.

Clamps and joints should be given careful consideration. The maximum annealing effect is apt to be found where the stresses in the conductors are at a maximum.

Surface measurement of conductor temperatures may be misleading by ten per cent, or more.

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Radio-Noise Filters Applied to Aircraft

C. W. FRICK
ASSOCIATE AIEE

S. W. ZIMMERMAN
MEMBER AIEE

THE importance of the use of radio in aerial navigation is well known. Of comparable importance to the operation of the aircraft, however, is the use of electricity in other forms, as for instance for engine ignition, for lighting, for control, and a variety of purposes. Since commutators and other forms of arcing contacts used in this equipment may cause interference with radio under some conditions, careful planning is necessary to get the fullest use from both power and radio equipment, and not overburden the aircraft with attachments solely to make the radio work.

In order to obtain installations which are reasonably free from radio interference of this kind, due consideration must be given to all of the component parts. For example, attention should be given to the shielding of radio equipment so that it will not be subject to direct pickup of radio noise phenomena. Furthermore, unnecessarily close proximity of power and radio circuits to each other, which gives close coupling for radio noise, should be avoided. In regard to the power equipment, disturbances which give rise to interference should be avoided as far as possible and the proper suppression and shielding employed where necessary.

With the understanding that proper attention is to be given the installation as a whole, this paper discusses the particular phase of the subject which deals with control of radio interference as a complex voltage on the power circuit called radio-influence voltage. Reducing or suppressing this voltage reduces the noise in the radio set resulting from this cause. The paper deals particularly with two suppression devices, capacitors, and radio-noise filters. Filters have been especially useful in many situations on aircraft equipment. They provide an effective and practical means of suppression for these applications.

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C. W. Frick is engineer in the general engineering laboratory, General Electric Company, Schenectady, N. Y. S. W. Zimmerman is research engineer in the lightning arrester department, General Electric Company, Pittsfield, Mass.

Available Filters

Manufacturers of filters have made available certain sizes of filters intended to cover a large majority of the requirements for aircraft. Filters are available in 25-, 50-, 100-, and 200-ampere ratings applicable to d-c circuits up to 50 volts. These filters are contained in a metal case which is provided with suitable mounting brackets.

These filters consist of a series line inductance with a capacitance at each end. Two terminals are arranged for connection of the filters in series with one side of the power circuit in which the noise volt-

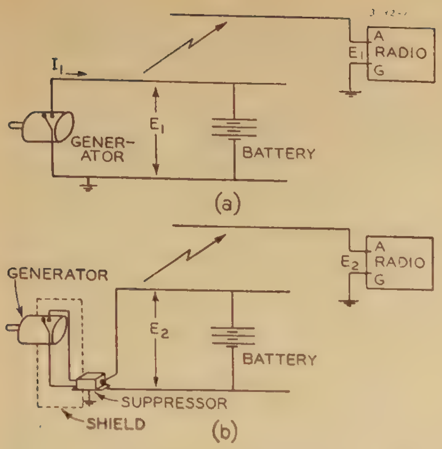

Figure 1. Elementary case of power circuit and radio receiving system

(a.) Without suppressor
(b.) With suppressor

ages are to be suppressed. The other side of the power circuit is grounded, and the filter case is also grounded. When tested in a circuit with aircraft equipment, noise-reduction ratios in the order of 200 to 1 to 5,000 to 1 (46 to 74 decibels) are obtained over the frequency range of 0.20 to 20 megacycles. Also on a circuit standardized for rating performance, noise reductions of 1,000 to 1 to 10,000 to 1 (60 to 80 decibels) are obtained over the same frequency range. Weights of these filters range between 1.5 and 3.0 pounds depending on current rating. Over all outside dimensions including mounting flanges range in length, breadth, and height from 4 by $3^3/_4$ by $2^1/_2$ to $5^1/_2$ by $4^1/_4$ by 3 inches, respectively. The filters will retain their suppression characteristics over a temperature range of 40 degrees centigrade below zero to 90 degrees centigrade above zero. Vibration tests in all possible positions of filter operation and at frequencies and amplitudes met with in service insure against mechanical failures.

Factors Entering into the Problem of Interference Control

The following discussion is intended to bring out by means of a simple example a number of points which should be understood in order to select, install, and test properly suppression devices, and some of the terms commonly used.

An elementary case of a power circuit and a radio receiving system is shown in the upper diagram *a* of Figure 1. The supply generator has a commutator which produces small but extremely rapid changes in the current and voltage. This causes a disturbance voltage and current, indicated at E_1 and I_1. In this diagram only the disturbance component is represented, the power voltage and current being omitted for clearness. The disturbance voltage and current result from a complex phenomenon involving very rapid changes and can, therefore, be treated as radio-frequency voltage and current, not of a single frequency but a spectrum covering a wide range of frequencies. The presence of this voltage and current E_1 and I_1 in the power circuit causes induction in the paralleling radio circuit, similar to the induction which takes place when a radio-frequency voltage is applied to the power circuit. The induced disturbance voltage which appears at the antenna and ground terminals of the radio set is indicated by e_1 and is called radio-noise voltage. The disturbance voltage on the power circuit which produces radio-noise voltage on an antenna, already described and indicated at E_1, is the radio-influence voltage (*RIV*). Both of these quantities can be measured in microvolts at various frequencies by means of a frequency-selective instrument called the radio-noise meter.[1]

There are several ways in which the possibility of interference with radio because of power equipment can be minimized, some of which are as follows. Referring to the elementary case of Figure 1*a*, it is evident that physical separation between the power circuit and the radio circuit would reduce the radio-noise voltage. However, space limitations in an aircraft make it impractical to control interference by this means alone. It is also evident that complete shielding of the radio system would eliminate or reduce the radio-noise voltage. An example of complete shielding would be a well-shielded, independently powered radio set with the antenna lead-in shielded up to the point where it emerges from the fuselage of a metal aircraft. A similar result would be obtained if it were practical to shield completely the power system by enclosing the generator, battery, and other apparatus in metallic containers and running connecting wires in continuous metal conduit. The shielding of power circuits carrying 100 amperes or more and extending to all parts of the aircraft has the disadvantage of weight and inconvenience in servicing the power system. However, complete shielding is commonly applied to some types of apparatus and circuits as, for instance, engine ignition.

Referring again to Figure 1*a*, it is seen that reducing or suppressing the radio-influence voltage E_1 reduces the radio-noise voltage e_1. This can be accomplished either by reducing the disturbance itself or by localizing, that is, confining the disturbance in the apparatus which produces it. A radio-noise suppressor is a device which does this. It is connected to a piece of apparatus such as a generator as indicated in the lower diagram *b* of Figure 1. It does not obstruct the flow of normal power but does prevent the flow of the unwanted disturbance. In terms of radio-influence voltage the purpose of the suppressor is to reduce the voltage E_1 of Figure 1*a* to a smaller value E_2 shown in Figure 1*b*. This results in reducing the radio-noise voltage e_1 to a smaller value e_2. The ratio E_1/E_2 called attenuation ratio is a measure of the effectiveness of the suppressor. It is often expressed in decibels (db) according to the relation

$$\text{db} = 20 \log_{10} \frac{E_1}{E_2}$$

This is called attenuation or insertion loss. The performance of a representative suppression device, in this case a filter, in a circuit such as Figures 1*a* and 1*b*, is shown by Figure 2 wherein the upper curve (E_1) shows the *RIV* without the filter and the lower curve (E_2) shows the *RIV* with the filter, plotted against frequency. The ratio of these two quantities, also plotted against frequency, is shown on Figure 3. Two vertical scales are given, one showing numerical ratio, and the other insertion loss in decibels.

Suppressor Arrangements

Suppressors have been applied to power circuits in many cases for such purposes as smoothing out ripples present in a d-c voltage or reducing distortion of an a-c voltage as well as reducing radio noise.

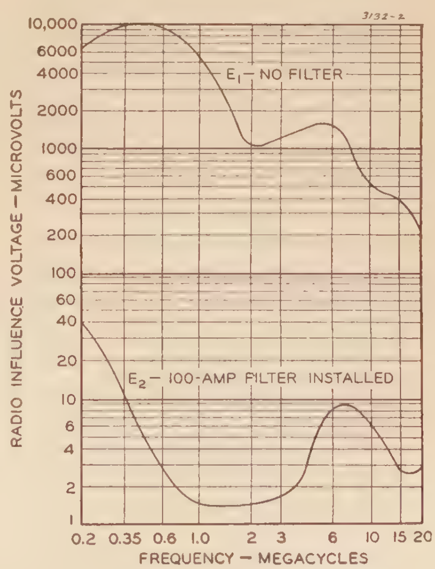

Figure 2. Radio-influence characteristics of airplane generator circuit

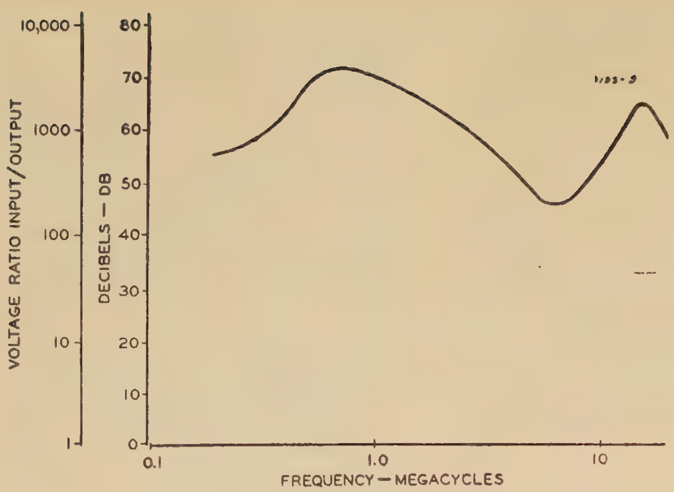

Figure 3. Attenuation characteristics of 100-ampere filter from generator test data

various other devices such as amplidynes, dynamotors, and inverters. Some of these pieces of apparatus have commutators, vibrating contacts, certain types of thermionic elements or other features required for their normal operation but which may give rise to radio-influence voltage. Tests have been made on apparatus representative of these various types, and it has been demonstrated that the selection of the proper suppression equipment depends upon the following:

1. The required attenuation of the radio-influence voltage.
2. The frequency range to be covered.
3. The impedance characteristics of the apparatus and circuit.
4. The power currents which the suppressor device must be able to carry.

While many different suppressor arrangements might be used, experience indicates that certain simple structures can be used to best advantage. Figure 4 shows a simple arrangement for smoothing out the ripple present in the voltage of a certain d-c machine. It consists of a shunt capacitor across the terminals. The capacitor does not pass direct current and hence does not interfere with the normal operation of the machine. In the diagram E_o represents the ripple voltage, Z_o represents the impedance of the machine for this ripple, and Z_c represents the corresponding impedance of the capacitor. It will be seen that Z_o and Z_c act as a potential divider for the ripple voltage E_o. For example, if Z_c is one tenth of Z_o, only one tenth of E_o appears on the outgoing circuit. In other words, this arrangement gives 10 to 1 reduction (20 decibels).

Sometimes it is advantageous to add impedance to the machine as at Z in Figure 5, especially if the machine itself has very little impedance. This arrangement is called an L filter. In many cases the shunt capacitor of Figure 4 or Figure 5 is replaced by a series tuned circuit called a resonant shunt. It reduces the harmonic in the same manner as the capacitor but is selective for a particular frequency. When several harmonics are involved, a separate resonant shunt is provided for each frequency. For a-c machines this scheme has the advantage of frequency discrimination between the fundamental and the harmonics.

A further arrangement which has been particularly helpful in the field of radio-noise suppression is the π filter shown in Figure 6. It acts as a two-step potential suppressor. Z_o and Z_{c1} constitute the first step which is similar to Figure 4. Z_{L2} and Z_{c3} constitute the second step. This affords further reduction. For example, if each step gives 10 to 1 reduction or 20 decibels, the over-all reduction will be 100 to 1 or 40 decibels. A practical aircraft filter of this type is shown in Figure 7.

The L filter and the π filter are similar in structure to certain simple filters known as low-pass filters employed in communication practice.

Use of Suppressors in Aircraft

The selection of a radio-noise suppressor to fit a given situation and the proper way to install it so that it will function properly involves a number of considerations which are discussed in the sections which follow. These considerations are brought out with reference to a type of aircraft power-supply system that has been studied extensively, but the conclusions are not necessarily restricted to this type of system. A common situation is that of a power supply consisting of one or more generators driven from aircraft engines and operated at about 28 volts d-c, a two-wire circuit, one side of which (usually the negative side) is solidly grounded to the body of the aircraft, stand-by storage batteries, motors, and

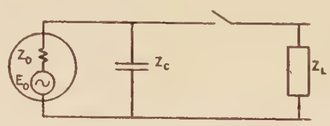

Figure 4. Capacitor as suppressor

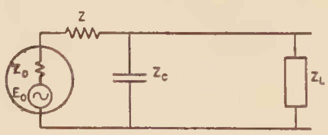

Figure 5. L filter as suppressor

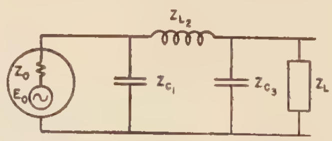

Figure 6. π filter as suppressor

Complete quantitative data on the first three items are not usually at hand, and most situations have to be treated on the basis of representative tests and experience with the use of suppression devices. With the aid of such knowledge, the kind of suppression equipment to employ can be decided upon. In some cases a capacitor as in Figure 4 can be used to best advantage. In other cases a filter arrangement such as Figure 5 or Figure 6 gives the best results. The general field of application for capacitors and for filters will now be considered.

Capacitors as Suppressors

There are many cases of devices having small current ratings such as five amperes or less on which capacitors may be used effectively, for example when the required attenuation ratio is between 10 to 1 and 100 to 1, and the frequency range is not large, such as the range of standard broadcast frequencies. A capacitor has the property of very low impedance at high frequency. In the case of an ideal capacitor the higher the frequency, the lower the impedance becomes. It must be remembered, however, that every resistor, inductor, and capacitor possesses to some degree all three of the properties—resistance, inductance, and capacitance. In the case of the capacitor some means of connection to the apparatus circuit is necessary, and these connections may have appreciable length and, therefore, appreciable inductance. To determine the effect of these properties a capacitor was tested in a circuit similar to Figure 4 with resistances used to represent the circuit impedances Z_o and Z_L. Voltage was measured with capacitor off and capacitor on. The ratio of voltage E_1 with suppressor off as in Figure 1*a* to E_2 with suppressors on as in Figure 1*b*,

which is the attenuation ratio is shown in Figure 8 plotted against frequency.

The straight line, dotted at the upper end, represents the calculated attenuation for a pure capacitance. The solid curve shows the actual attenuation. It will be noted that the attenuation over the lower part of the frequency range coincides with the pure capacitor, but the attenuation at the upper end is quite different and is characterized by a peak at point *A*. At this point actual performance exceeds the calculated performance based on pure capacitance mainly caused by capacitor resonance, but there is a rapid falling off of attenuation ratio at frequencies above this peak, instead of an increasing attenuation ratio as there would be with an ideal capacitor. The frequency at which the peak occurs in the case of the capacitor tested, which was of a special design for high frequency, was between five and ten megacycles. The characteristic of another capacitor of the same microfarad value but not specially designed for high frequency is shown by the broken line for comparison. The shape of the latter curve indicates that the capacitor has somewhat more inductance and more resistance at high frequencies, which limit its effective range to about two megacycles.

Referring to the "high-frequency" capacitor, the maximum effectiveness, and the frequency at which it occurs, at *A*, depend upon all three of the quantities—resistance, capacitance, and inductance. In this frequency region increasing the capacitance does not necessarily increase the effectiveness and may result in reduced effectiveness depending on the other factors. Sometimes advantage is taken of the "peak" effectiveness by choosing suitable values of capacitance and length of leads to control the inductance so that the peak occurs at a desired frequency. The usefulness of this scheme is somewhat limited, and it is generally preferable to make the capacitor leads as short as possible.

The curve of Figure 8 indicates in a general way the field of application for capacitors as suppressors. At the lower end of the curve which in this example corresponds to about 0.2 megacycle, the capacitor has less effectiveness than at the higher frequencies. While the effectiveness at this frequency can be improved by making the capacitance value (microfarads) larger, this usually results in considerable size and weight. A further increase in attenuation can be obtained by means of a filter such as Figures 6 and 7 with less bulk and weight. At the upper end of the frequency range, that is, above five or ten megacycles, the effectiveness of the capacitor is limited by the inductance of leads and other factors besides capacitance. In and above this frequency range it is usually necessary to use a filter giving an attenuation curve such as Figure 3. In the frequency range from about one to five megacycles there are many cases where the capacitor as a suppressor is the best solution. The most important considerations in the use of capacitors as suppressors are the characteristics of these capacitors in the desired radio-frequency band and the proper arrangement of leads and connections.

Figure 7. Radio-noise filter showing compact arrangement required

Whether capacitors are used instead of filters depends on

(*a*). The economies.

(*b*). The allowable weight and size.

(*c*). The required performance.

Filters As Suppressors

When apparatus rated one kw or more has to be suppressed over a wide range of frequencies such as 0.2 to 20 megacycles and suppression of 40 decibels or more is required, a capacitor is not generally sufficient for reasons already shown, and some form of filter is necessary. While in some instances filters have been specially designed and assembled as part of a piece of equipment, the general situation calls for the availability of filters as separate items to be selected and applied as the need arises. Manufacturers have made available certain sizes of filters intended to cover a large majority of these requirements. To meet as nearly as possible the general requirements outlined, these filters are designed to give attenuation characteristics based on measurements on a variety of apparatus and experience of aircraft operators over the frequency range of radio equipment used in aircraft. Impedance characteristics of aircraft equipment and circuits have been taken into account in a general way by the use of a laboratory test setup to be described later.

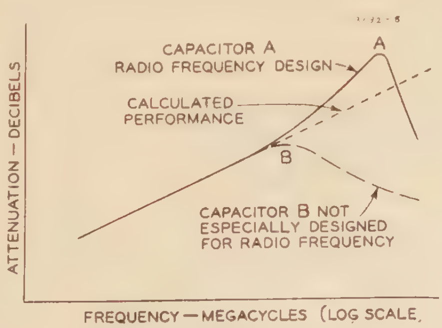

Figure 8. Radio-influence voltage suppression characteristics of capacitors

To illustrate the application of these filters the case of a 2.5-kw 28-volt d-c generator will be considered. The full-load current of this machine is 90 amperes approximately. The filter rated 100 amperes would, therefore, be selected and installed as illustrated in Figure 1*b*. Filter performance is illustrated by Figure 2 already referred to in this paper, when the filter is properly installed.

In order to obtain the full degree of effectiveness which the filter is capable of giving, careful attention to certain installation details is necessary. While a generator is referred to as an example the discussion is intended to apply to a variety of apparatus. The filter should preferably be close to the generator. The intervening circuit must be shielded, the enclosure generally consisting of a conduit which forms a continuous shield with the generator and filter cases. The filter base must be well grounded, for instance mounted directly on the grounded metal structure. The grounded side of the circuit should be well grounded at a point immediately adjacent to the filter.

The importance of this and other factors to the proper functioning of such a filter can be seen by reference to Figure 9. Here three important factors are emphasized, all of which are usually present to a greater or less degree simultaneously. In these diagrams the filter is intended to isolate the radio-influence voltage appearing at the left from the circuit at the right but is not fully effective because of faulty coupling.

Capacitive coupling shown at *a* in Figure 9 is one of the conditions that may interfere with predicted good performance. It results from the presence of ex-

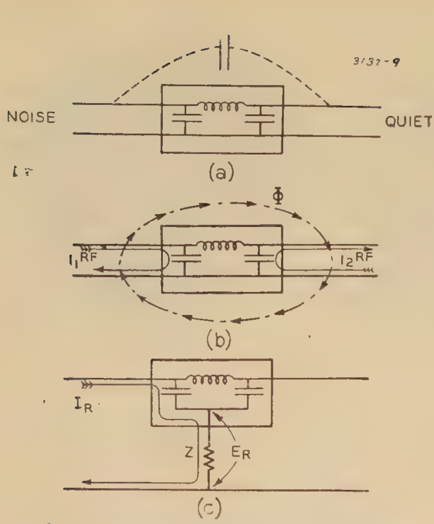

Figure 9. Types of faulty coupling in filters

traneous capacitance between input and output circuits as indicated by C. To high frequencies this provides a by-pass and allows extraneous voltages to appear in all or certain parts of the quiet circuit in spite of the presence of the filter.

Inductive coupling shown at *b* in Figure 9 is another condition to be avoided. If the impedance to radio-frequency currents flowing at the left is small, as it should be for a well-designed filter intended for suppression, radio-frequency currents flowing through this circuit may

Figure 10. Insertion-loss tester for radio-noise filters

set up electromagnetic flux which if allowed to cut the conductors of a quiet circuit will introduce therein a radio-frequency disturbance as illustrated. In many cases magnetic shielding can be used to reduce this coupling. Electromagnetic flux at commercial frequencies must be shielded with heavy sections of magnetic material. At radio frequencies flux penetration can be prevented to a great extent with thin conducting surfaces. Inductive coupling occurs only when there are loops existing in input and output circuits.

Conductive coupling is a condition such as shown at *c* in Figure 9. Radio frequency currents in the circuit at the left flow through a common ground lead having an impedance Z. As a result voltage E_R appears across this impedance and is directly introduced in the circuit which is intended to be quiet. This type of induction may occur unless particular care is taken to secure a good ground connection. The case of the filter should be solidly connected to the electrical ground plane. The impedance of only a few inches of ground wire is extremely detrimental to filtering action.

To obtain a quiet circuit all sources of noise must be given attention. One filter can sometimes be arranged to suppress more than one source. For instance, if the generator in the aforementioned example has a voltage regulator which causes radio noise and all connections to this regulator are made between the generator and the filter, the filter suppresses noise from both sources. All other connecting wires such as field leads must also be connected on the generator side of the filter.

In general the important things to be given attention in the installation of a filter are location, shielding of the unfiltered parts of the circuit, and ground connections.

Measurements

Tests are needed to check the performance of filters under actual operating

Figure 11. Insertion-loss tester for radio-noise filters

Rear view showing amplifier, oscillator, and power supply

conditions and to obtain data on which further designs and improvements can be based. The effectiveness of filters is determined by making two measurements of radio-influence voltage preferably on an actual installation in an aircraft. These measurements are made

(*a*). Without filter in the circuit.

(*b*). With filter in the circuit.

Measurements on the input and output sides of the filter do not indicate the effectiveness of the filter because the filter itself changes the circuit constants. The necessary equipment for these tests consists of a radio-noise meter[1] and a reliable signal generator for calibration.

To get measurements within the accuracy[2] of the noise meter certain precau-

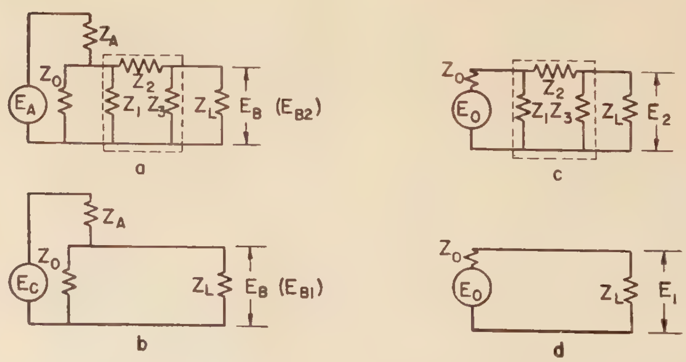

Figure 12. Laboratory circuit for rating filter performance and corresponding apparatus circuit

(*a*). Laboratory circuit, filter in

(*b*). Laboratory circuit, filter out

(*c*). Apparatus circuit, filter in

(*d*). Apparatus circuit, filter out

tions are necessary, particularly in tests made on an aircraft in flight. Among these are

(*a*). Maintenance of proper battery voltage.

(*b*). Proper alignment and adjustment of the noise meter and frequent calibration checks against the signal generator.

(*c*). Placing the noise meter close to the test point which in measurement with the filter should be right at the filter terminal on the "quiet" side.

(*d*). Avoiding test locations where the noise level is high.

(*e*). Providing the noise meter with cushions to eliminate mechanical vibration.

(*f*). Providing a temperature controlled chamber when low temperatures are encountered.

In addition to tests on the actual equipment it has been found useful to make tests on filters at radio frequencies in a laboratory circuit. This setup approximates in a general way the impedance characteristics of a piece of aircraft equipment and the asssociated circuit. The filter performance curves so obtained are not interpreted as those to be expected on apparatus but rather as those obtained in a standardized test circuit. However, the general experience has been that the filters showing the best performance on this network also showed the best performance in actual apparatus. This test has proved useful in the development of filter designs and in checking the product of the factory. Figures 10 and 11 show a filter tester based on this method used in a factory.

A diagram of the laboratory circuit in somewhat common use is shown in Figure 12 together with the corresponding apparatus circuit. Filter performance in this circuit is obtained by measurements without the filter in the circuit and with the filter in the circuit. The analysis given in the appendix shows that the laboratory circuit indicates the same performance as an actual apparatus circuit with a certain set of impedance values.

The measurements just described on a laboratory circuit or on the actual power circuit do not take into account the radio equipment and circuits. A complete study of filter performance should include measurements on the radio equipment, but consideration of the measurement equipment and technique for this purpose is beyond the scope of this paper.

Appendix. Laboratory Circuit for Filter-Performance Tests

The attenuation test commonly made on filters in the laboratory with sine wave voltages at radio frequencies utilizes a resistance network as shown in diagrams *a* and *b* of Figure 12. The voltage source or signal generator is represented by E_A. The circuit impedances Z_o and Z_L of a corresponding apparatus circuit shown by diagram *d* are represented by the same symbols on diagrams *a* and *b*. In the laboratory test these impedances consist of 20-ohm resistors. In addition a 300-ohm resistor Z_A is connected in series with the signal generator to adapt the circuit to the internal impedance characteristics of available signal generators. This 300-ohm resistor does not affect the test result because it cancels out in the "filter out" to "filter in" ratio as shown by the formulas in this appendix. In the diagram *a*, Z_1, Z_2, and Z_3 represent a π-filter (see Figure 6).

The test may be made in two ways. The first method is to hold the signal generator voltage constant and obtain two values of output voltage E_B, E_{B1} with filter out as shown in *b* of Figure 12 and E_{B2} with filter in as shown in *a*. The ratio $(E_{B1})/(E_{B2})$ is equal to the filter attenuation ratio, as will be demonstrated. The second method will be described later.

The following approximations which hold for practically all test setups have been made for the purpose of simplifying the analysis:

The paralleling effect of Z_L on Z_3 is neglected.

The paralleling effect of Z_o (to the left) and of other impedances (Z_2 and so forth to the right) on Z_1 is neglected.

The impedance looking into Z_2 from the left, diagrams *a* and *c*, is assumed to be the same as Z_2 itself, which neglects the added effect of Z_3 and Z_L.

The impedance looking into the circuit from the signal generator in *a* and *b* is assumed to be equal to Z_A which neglects the added effect of Z_o, Z_1, Z_L, and so forth. The largest error occurs in *b* where the actual value is 310 ohms and the approximate value is 300 ohms, the value of Z_A.

The impedance looking into the circuit from the source in diagram *c* is assumed to be equal to Z_o which neglects the added effect of Z_1 and other elements to the right.

In diagram *a* the value of E_B is approximately

$$E_{B2}=E_A\frac{Z_1}{Z_A}\times\frac{Z_3}{Z_2} \tag{1}$$

When the filter is removed, as in *b*, and the signal generator voltage kept the same ($E_C=E_A$) E_B becomes approximately

$$E_{B1}=E_A\frac{Z'}{Z_A} \tag{2}$$

where Z' represents Z_o and Z_L in parallel.

Combining these equations we have

$$\frac{E_{B1}}{E_{B2}}=\frac{Z'Z_2}{Z_1Z_3} \tag{3}$$

It will be shown that this equation gives the same ratio as the voltage ratio E_1/E_2 in the apparatus circuit diagram *c* and *d*. In the former (filter in) the voltage E_2 is given approximately by the relation

$$E_2=E_o\frac{Z_1Z_3}{Z_oZ_2} \tag{4}$$

In diagram *d* (filter out) the voltage E_1 is given by the relation

$$E_1=E_o\frac{Z_L}{Z_o+Z_L} \tag{5}$$

Combining equations 4 and 5 we obtain

$$\frac{E_1}{E_2}=\frac{Z_oZ_L}{Z_o+Z_L}\times\frac{Z_2}{Z_1Z_3} \tag{6}$$

Since the term $(Z_oZ_L)/(Z_o+Z_L)$ gives the value of Z_o and Z_L taken in parallel which is designated by Z' in equation 3, the right-hand members of equations 3 and 6 are the same, and therefore

$$\frac{E_{B1}}{E_{B2}}=\frac{E_1}{E_2} \tag{7}$$

Equation 7 shows that within the approximations stated the laboratory test shows the effectiveness which the filter being tested would have in an apparatus circuit having the same value of Z_o and Z_L.

It is generally more convenient to use the second method which is to reduce the signal generator voltage in diagram *b*, filter removed, to give the same value of output voltage E_B as in *a*, filter in. The effectiveness is then given by E_A/E_C which is the same ratio as $(E_{B1})/(E_{B2})$. This is demonstrated as follows:

Equation 1 becomes $E_B=E_A\dfrac{Z_1Z_3}{Z_AZ_2}$ (1a)

Equation 2 becomes $E_B=E_C\dfrac{Z'}{Z_A}$ (2a)

Combining these equations we obtain

$$\frac{E_A}{E_C}=\frac{Z'Z_2}{Z_1Z_3} \tag{3a}$$

Referring to equations 3 and 7, it is evident that

$$\frac{E_A}{E_C}=\frac{E_1}{E_2} \tag{8}$$

This relation enables the filter effectiveness to be determined from the signal generator voltage calibration in which case the voltmeter on the output (E_B) is used merely as an indicator.

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1. Methods of Measuring Radio Noise 1940, report of Joint Co-ordination Committee on Radio Reception of Edison Electric Institute, National Electrical Manufacturers' Association and Radio Manufacturers' Association. Edison Electric Institute publication G9, National Electrical Manufacturers' Association Publication 107, Radio Manufacturers' Association *Engineering Bulletin number* 32.

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Manual Switches for Aircraft

R. A. MILLERMASTER
MEMBER AIEE

Synopsis: In the design and application of electrical control equipment the problem of obtaining complete field data and requirements is always present in some degree.

The problem is acute in branches of the electrical industry supplying aircraft control. Three factors contribute to the difficulty:

1. The tremendous engineering effort centered in the aircraft industry causes rapid technical changes that soon make accumulated data obsolete.

2. Technical data on utilization devices are not always available to either the aircraft or electrical manufacturer. The mushroom growth and technical advance of the aircraft industry have, in some cases, outdistanced laboratory equipment and personnel capacity so the data an electrical-control manufacturer would like to have are simply not available.

3. The secrecy surrounding many aircraft developments blocks the passing of information from the aircraft manufacturer to the electrical manufacturer.

The electrical-control manufacturer can meet this problem, in part, by making available complete performance data on switching equipment offered for sale. Properly chosen, these data enable the aircraft engineer to match control means with his electrical utilization device requirements.

In the preparation of such data the following considerations are of primary importance to both the user and producer of switches.

The functions of an aircraft switch may be classed broadly as follows:

1. To establish a circuit.
2. To maintain a circuit.
3. To interrupt a circuit.

Performance of these functions is importantly affected by the following characteristics of the load:

(*a*). Inrushes.
(*b*). Inductance.
(*c*). Current.
(*d*). Voltage.

IN establishing a circuit, the increasing order of difficulty shows:

(*a*). Inductive loads.
(*b*). Resistive loads.
(*c*). Lamp loads.

When a set of switch contacts closes, the circuit is seldom established without some disturbance (see Figure 1). The current magnitude at the time of this disturbance becomes a major factor in switch performance. Contact erosion results, caused by the arcing which normally accompanies such disturbance, and in extreme cases freezing or welding occurs with the possibility that the switch may immediately become inoperative through inability to open the freeze. In most switches the contact bounce that creates these disturbances ends within ten milliseconds, and the current magnitude for that period of time is of primary importance in connection with this first function of a switch, establishing the circuit.

(*a*). Inductive loads build up slowly, and the current for a time after the switch contacts close is less than steady-state value.

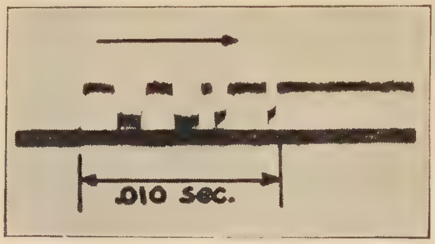

Figure 1. Switch contact bounce

For this reason damage to contacts is minimized.

(*b*). Resistive loads cause more damage in establishing the circuit than inductive loads do because of the substantially vertical front current increase. This means that any bouncing of contacts (with accompanying arcing) occurs at practically full steady-state circuit load instead of the fraction of steady-state current indicated in the preceding inductive-load discussion.

(*c*). Inrush loads are most severe in circuit establishment. The common forms experienced are lamp and motor inrushes. The former—because of their characteristic vertical front current increase, and because of the relatively slow decay of the inrush—are the most severe of any circuit-closing condition. Any switch bouncing is almost sure to occur at currents well above steady-state values. Motor inrushes are not comparably severe because the inductive component tips and rounds off the wave front in a manner so bouncing usually occurs before maximum inrush current is reached. Condensers have a steep wave front like resistance, but the circuit ordinarily has other limiting resistance.

The second function of the switch, maintaining the circuit, is mainly affected by the circuit characteristics of current and voltage. The passage of current across the internal resistance of a switch results in a wattage loss that must be dissipated without causing harmful temperature rises in either conducting or insulating members. Experience indicates that, if the continuous duty rating of a switch be taken as 100, the 15-minute, 5-minute, 1-minute, and 5-second ratings may be taken as 110, 150, 200, and 400, respectively, for the average switch used in aircraft circuits. Losses must also be limited to the extent necessary that excess voltage drop will not obtain. The circuit voltage (which may vary from supply voltage) strains the switch insulation, which must, under all operating conditions, be capable of restricting current flow to switch parts that are normally current-carrying members. Conducting paths to ground or opposite polarity result from voltage breakdowns, and such paths may carbonize and progressively develop more wattage until complete failure occurs. The stripping of conductor insulation at the switch requires the provision of adequate circuit insulation from terminal to terminal through the switch.

The third function of the switch, interrupting the circuit, is affected by circuit current, voltage, and inductance. Figure 2 shows a switch opening a resistance load of 30A at 30V and the same switch opening a highly inductive load of 25A at 30V. The arcing time for the inductive but slightly smaller load is more than 12 times that for the resistive load. The major watt-seconds loss is developed near the start of circuit opening even though a high-voltage kick develops just before the current reaches zero. This last kick, though occurring at so low current value that little wattage is involved is of importance because of the strain it places on circuit insulation and because it contributes, in some instances, to arc restriking or flashovers to ground.

In order that switch functioning under the previously described circuit conditions may be checked by laboratory tests it is necessary that voltage and current

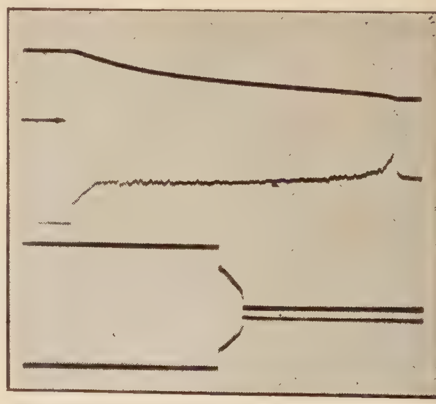

Figure 2. Current and voltage across switch contacts, 25 amperes inductive (above) and 30 amperes resistance (below) loads on 30-volt supply

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R. A. MILLERMASTER is supervising development engineer for Cutler-Hammer, Inc., Milwaukee, Wis.

sources, along with elements to establish circuit components for inrush and inductive effects, be provided. A series of performance requirements, substantially formalized in specification *ANS*-20 is applied to test each switch intended for aircraft, establishing the following data:

1. Current-making capacity (10,000 operations).

(*a*). Lamp load (10× hot filament current).

2. Current-breaking capacity (10,000 operations).

(*a*). Resistance load.

(*b*). Inductive load (using standard test inductors with L/R ration of 0.26).

3. Emergency break (50 operations inductive load).

4. Current-carrying capacity.

1*a*. The inrush circuit provided for determining current-making capacity uses a normally closed contactor which opens soon after (about 40 milliseconds) the switch under test closes (see Figure 3) and remains open until the switch under test opens. As shown in Figure 4, the switch under test opens zero load. This may be made a finite load, however, by shunting the break load across this normally closed contactor. The advantage of this circuit lies in the fact that the line of current increase is almost vertical, and a flat top holds the inrush at maximum value for approximately 40 milliseconds so that any switch closing disturbance occurs at peak current. The lockout-contactor type of circuit is a more severe circuit than usually found in practice, but it provides a test that may be duplicated in any laboratory and determines a switch rating that may be used safely with any kind of inrush load.

2*a*. The resistance-load test requires no special comment.

2*b*. The inductive circuit for this type of testing was co-operatively developed by the Army Air Corps and the Navy Bureau of Aeronautics, again with the aim of providing a circuit which could be duplicated in any laboratory and which would, at one time, test switches under conditions of sufficient severity to guarantee satisfactory performance in any of the so-called "highly inductive" aircraft circuits. These circuits include brake coils, solenoids, relay and contactor coils, and so forth.

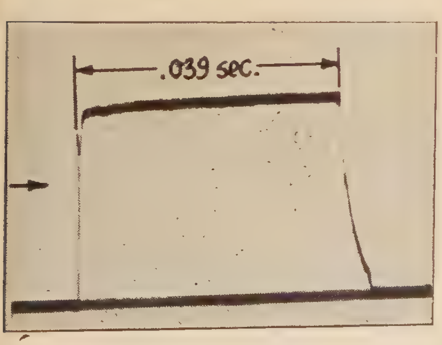
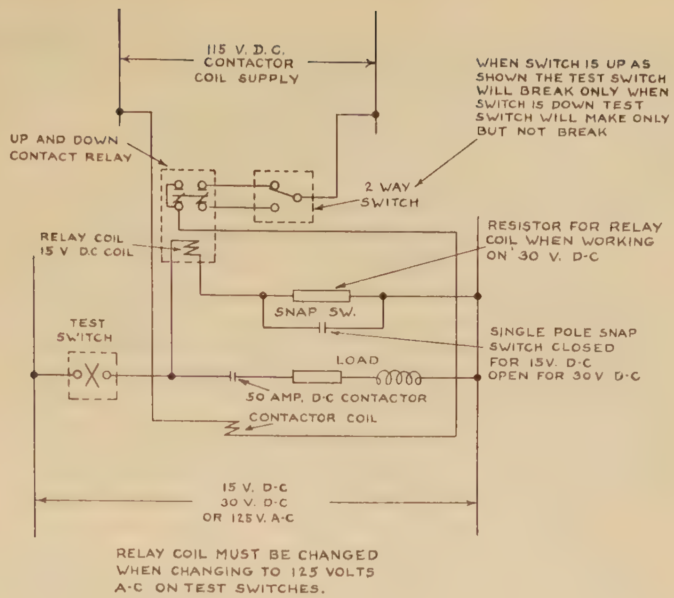


Figure 3. Wave form. Standard inrush circuit

Figure 4. Line diagram of standard inrush circuit



So duplication could be readily attained, the following inductor elements are defined:

1. Iron circuit.

(*a*). Lamination dimensions.

(*b*). Lamination material.

(*c*). Magnetic saturation.

(*d*). Air gaps.

2. Copper circuit.

(*a*). Coil dimensions.

(*b*). Ballast resisters.

The stored energy of the magnetic circuit and the rate at which this stored energy is released were made the subject of a study based on tests conducted on a number of solenoids, relay coils, and so forth, now in use. Voltage surge, current decay, and arcing time were the main measures of equivalence and were determined with the circuit controlled by the same switch type in all cases. The standard test inductor shown in Figure 5 resulted.

The functioning of a switch is basically dependent on the characteristics of the electrical circuit to be controlled, but the physical characteristics of the surrounding atmosphere and supporting structure are, in many cases, of equal importance.

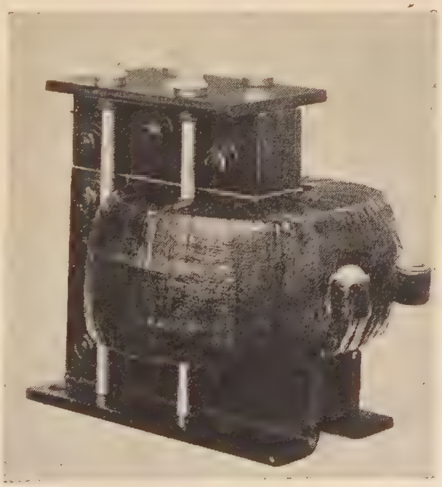


Figure 5. Standard test inductor

Physical conditions which at times assume such importance are:

1. Ambient temperature.
2. Humidity.
3. Chemical atmosphere or sprays.
4. Accelerations.
5. Altitude or pressure.

The ambient temperature, in the low range of 50 and 60 degrees Fahrenheit below zero may interfere with the mechanical action of a switch by congealment of lubrication, while the high range of 150–175 degrees Fahrenheit accentuates the problem of temperature control in current-carrying elements and insulation.

The second physical condition substantially affecting switch performance is the presence of humidity or moisture, either through exposure of the switch to direct wetting action by rain, splashing, and so forth, or by breathing air into the switch interior under conditions of high relative humidity with variable temperature. In the first case the switch is best protected by the use of water-resistant gasketing and joint constructions. In the

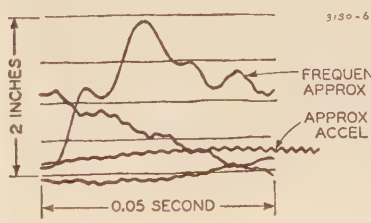


Figure 6. Panel subjected to shock test

With eight-pound shutter on bakelite panel at center

second case, with moisture in a relatively tight switch interior by condensation of vapors entering the switch by breathing, either the provision of drain holes or a highly water-resistant construction is indicated.

The third condition involves the presence of salt spray or combinations of chemical atmospheres with heat or moisture that result in deterioration at metallic surfaces and junctions of case or mounting elements. The elimination of abutting unlike metals or metals with unlike plating is a partial solution to this problem although the protective adequacy of the plating as related to the base metal must be determined. Nickel on steel or cadmium on brass are not the best specific protective coatings for these two base metals, yet cadmium plating for assemblies of steel and brass parts is a good plating because the more readily corroded material (steel) is protected and unlike metallic abutments are not present. Choice of protective metallic coating must also recognize the by-products resulting from protective action. For instance, zinc, in the presence of moisture, forms zinc hydroxide and carbonate, bulky salts of high electrical resistance, that would cause failure in closely fitting assemblies or at the electrical contacts of a switch. Cadmium, in a tight enclosure and at high temperature, in presence of some insulating varnishes and oils, such as tung and chinawood oils and air dry phenolic varnishes, may "bloom," forming a bulky salt of high electrical resistance.

The fourth condition affecting switch performance results from mechanical acceleration which may occur in the following forms:

(*a*). Linear.
(*b*). Vibrational.
(*c*). Shock.

(*a*). In the first form a constant linear acceleration and its effect on switches can be readily computed and measured by laboratory tests. A rotating table may be used to generate this acceleration in accordance with the formula:

$$\text{Acceleration} = Kx(\text{RPM})^2 \times R$$

(*b*). In the second form of acceleration free vibrations that may be induced by vibration in the plane frame and structural elements are checked by test on a table oscillating in harmonic motion over a frequency range of 10 to 60 cycles per second with an over-all amplitude of 0.060 inch. Devices under test are checked for structural failures and electrical failures (closing circuit when open or vice versa) with the frequency range traversed at the rate of about once in 3–5 minutes for a period of one hour.

(*c*). In the third form of acceleration, shock resulting from landing, shell burst, and so forth, transmits itself to the switch through plane structure and switch mounting means. The motion, velocity, and accelerations involved are a combination of effects discussed in *a* and *b*, although the energy imput to the switch is ordinarily greater than in either *a* or *b*. See Figure 6 showing the time—motion trace of a panel subjected to a high-shock test. Manual switches have been tested in a pendulum shock machine. Consideration is being given the use of a drop machine suggested by the Bureau of Standards.

In these three acceleration tests, the measure of electrical failure is a self-maintaining electrical indicator set to pick up in four milliseconds and drop out in four milliseconds. A device like a bomb rack solenoid requiring eight milliseconds to initiate armature movement, would not be affected when in circuit with a switch which passes a shock test with four millisecond indicating means.

The fifth physical condition affecting switch performance is altitude or pressure. Air-gap dielectric strength becomes a matter of definite concern. The sparking potential, a function of gap and pressure, becomes a major factor, and switches with normal forms of air gaps at contacts and terminals, capable of holding 2,500 volts pressure across these gaps at 29 inches of mercury, have by test failed at 800 volts in pressure equivalents of five inches of mercury. In the presence of such a substantial drop in sparking potential any tendency toward flashovers to ground or across polarity during circuit opening is severely emphasized, and the interrupting capacity of switches, particularly on inductive circuits, must be carefully determined.

Aircraft Inverter Construction

C. T. BUTTON
ASSOCIATE AIEE

THE word "inverter" is a generic term used to designate any device which receives direct current and delivers alternating current. Such a device may consist of electronic elements, or vibrating contacts, or mechanical choppers, or rotary types. The scope of this paper will be confined to a discussion of rotary-type conversion equipment.

Various classes of rotary inverters and control accessories will be listed with a brief and general description of characteristics and relative merits, viewed from the standpoint of the requirements of military aircraft. A statement of what the principal requirements are is first in order.

Operating Requirements

An aircraft inverter must operate on the d-c supply of the plane, which usually is obtained from generators driven from the main engines. These generators may be of 50-, 100-, or 200-ampere rating; and in a four-engine bomber there are usually four 200-ampere generators connected in parallel while in normal flight. The d-c bus voltage is maintained fairly constant by means of carbon-pile regulators controlling the generator fields. A battery usually floats on the bus.

The inverter is connected to the d-c bus through a solenoid-operated switch. For purposes of inverter design, the voltage delivered to it is considered to be 27.5 volts, with maximum variation of plus or minus 2.5 volts. In routine flight, carbon-pile voltage regulators restrict the d-c voltage variation to less than one volt, except for momentary transients.

With an input voltage at any point from 25 to 30 volts, the inverter should be capable of starting and carrying full rated load continuously. An operating life of at least 1,000 hours without major overhaul is expected, with starting as frequent as once per hour.

Output ratings may be anywhere from 6 to 3,000 volt-amperes, with an ever-present possibility of increased loads in the future requiring larger sizes. Effort has continuously been made and will continue to be made to standardize on rated loads or sizes. In view of the fluidity of the load requirements and resulting necessity of modifying specifications on actual conversion units procured from time to time, a formal tabulation of ratings will not be listed. Formalized specifications are available, one of which is the United States Army Specification 94-32270. These formal specifications are amended from time to time as need requires.

In order that this discussion may be self-contained however, three sizes of

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C. T. Button is sales engineer for the Holtzer-Cabot Electric Company, Boston, Mass.

inverters currently used in large quantities will be mentioned, the ratings of which are as follows:

Rating	Output at 400 Cycles: 26 Volts 40 Per Cent Power Factor	Output at 400 Cycles: 115 Volts 90 Per Cent Power Factor	Class*
250 va.	60 va. . 1 phase	190 va. . 1 phase	C
750 va.	250 va. . 1 phase	500 va. . 1 phase	D
1,000 va.	250 va. . 1 phase	750 va. . 3 phase	F

* See tabulation under "Classification of Basic Types."

The 26-volt output is primarily intended for electrical-instrument supply but may be used for other single-phase loads such as fluorescent lighting. The voltage requirements are not exacting on this circuit, and automatic regulation has not been considered necessary.

The 115-volt circuit may be used to supply fluorescent lights, automatic radio compass, motor loads, supercharger regulators, automatic pilot, and bombsight. In the case of some of these loads, the voltage requirements are not exacting, and the percentage variation experienced on the d-c bus may be tolerable. In such case, an inverter which may be regarded as a transformer (if of sufficiently low internal impedance) may be a simple and satisfactory mechanism.

However, as more precision equipment is developed and put into use, and with transient voltage fluctuations occurring because of high momentary current to gun-turret motors, need for automatic control of the 115-volt 400-cycle source becomes apparent.

As a broad statement, the voltage on that circuit should not exceed 120 nor be less than 110 at any load from no load to full load, and at any d-c input voltage from 25 to 30. However, modulations or transients or poor wave form may be objectionable in some instances. Since no practical source will deliver a pure sine wave at exactly 115 volts 400 cycles in service, and since the greater precision demanded the more complicated the equipment becomes, the construction and acceptance of the most practical machine in any particular case necessarily be-

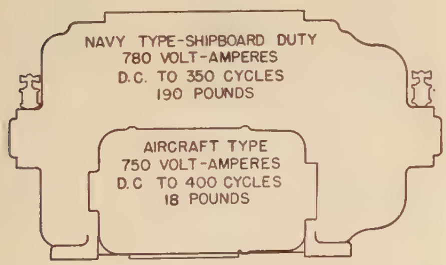
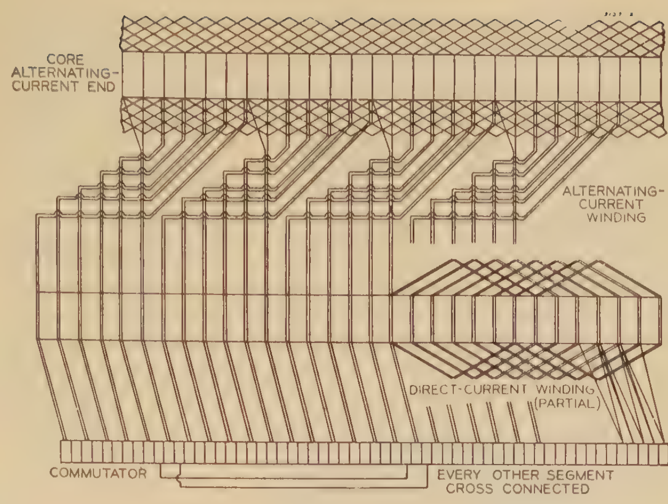


Figure 1. Comparison of size and weight of units built for aircraft and shipboard duty

Figure 2. Cascade inverter — schematic diagram of armature connections



comes a matter requiring engineering cooperation. In other words, there are so many factors involved that the procurement engineers cannot say "Here are our specifications. Any machine meeting them is acceptable, and contrariwise." Likewise, the manufacturers' engineers cannot say "Here is the best possible equipment for the job; that's all there is."

Specified weights for various inverter ratings will not be listed, as they are changing from time to time. Refinements in design make lighter weight machines available. However, a contrary tendency toward heavier weights also inevitably results from requirements for more accurate control of output voltage and frequency. As an illustration of weight trend, one inverter of the inductor-dynamotor type was in use in 1939, weighing 18 pounds and capable of delivering 250 volt-amperes. A cascade-type inverter was designed in 1940 having the same weight and delivering 750 volt-amperes. A comparison of the latter with a motor-generator set for use on board ship is given by Figure 1, where the two are shown with outlines drawn to the same scale.

Later developments, however, required addition of radio-interference filter and control accessories, adding a few pounds.

The importance of light weight in aircraft equipment needs no argument. One pound of fixed weight requires another pound of structure and fuel to carry it.

Likewise, the importance of efficiency may be emphasized by the following approximate data:

An aircraft engine consumes about 0.5 pound of fuel per horsepower-hour. The generators are roughly 75 per cent efficient. Hence, 1,000 watts of inverter input requires the consumption of about one pound of fuel per hour. Every 100 watts saved is worth a pound of fuel on a ten-hour flight. That pound of fuel might take the airplane another half mile. It might be said as a generality that 100 watts of additional inverter losses would necessitate a certain additional weight of generator capacity. In a practical case, this capacity may be already available.

All things considered, perhaps a weight saving of one pound in an inverter is worth while if the losses are not increased as a result by more than 100 watts. This figure is by no means definite, however, and has not been set up as a design criterion.

It is no simple problem, nor can any definite general answer be given, as to what point it is best to stop at in weight reduction on account of loss in efficiency or possible sacrifice in reliability. Likewise, extremely light weight may mean high internal impedance and hence high-voltage regulation. High speed makes light weight possible but reduces bearing life and decreases reliability.

These are all matters which should have the benefit of sound engineering judgment. The most sound judgment is

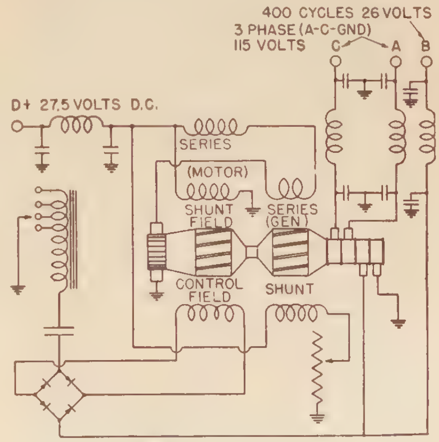


Figure 3. Schematic diagram of 1,000-volt-ampere dynamotor-generator type

based on experience. It is quite common in aircraft work, however, for experience to bring to light considerations which either were not anticipated or were erroneously regarded as of negligible importance.

Classification of Basic Types

A rotary inverter essentially consists of a d-c motor driving an alternator. The two elements may be combined in a

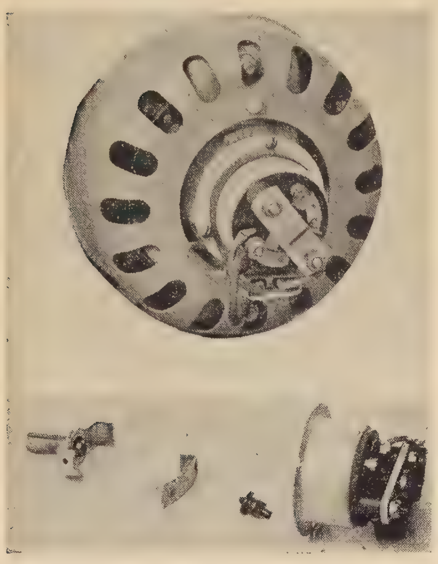


Figure 4. Centrifugally operated contact-making speed governor, cover removed

Parts shown in lower view include two rotating pieces, bakelite button for transmitting thrust, and stationary contact unit with adjusting screw

single magnetic circuit, the most intimate combination being an inverted rotary converter. The latter is analogous in some degree to an autotransformer, where primary and secondary conductors are common. A simple rotary converter cannot be used to fulfill the requirements in question, however, on account of the voltage ratio involved and the fact that grounded circuits are used on both primary and secondary.

The following list covers classes of equipment in current production, types which have been built experimentally, and others which conceivably could be used. The list does not include every theoretical possibility of variation and combination.

A. Motor-generator set

1. M-G set with rotating field generator
2. M-G set with rotating armature generator
3. M-G set with inductor generator
4. M-G set with permanent magnetic generator

B. Dynamotor

C. Inductor-dynamotor

D. Cascade inverter

E. Inverted rotary converter with transformer

1. Separate transformer
2. Rotating transformer

F. Dynamotor-generator

G. Dynamotor with booster

H. Cascade inverter with booster

Types *A*4, *B*, *C*, *D*, and *E* are not adaptable to independent output voltage control, except by a separate series boost or buck arrangement in primary or secondary circuits. The behavior of *B*, *C*, *D*, and *E* types may be compared with that of a transformer, where output voltage is proportional to input voltage less internal impedance drop.

Considering the other classes where automatic control of output voltage may be obtained by field regulation, these may in turn be divided into two groups—those with slip rings and those without.

*A*1, *A*2, *F*, and *G* have slip rings.

*A*3 and *H* do not have slip rings.

Of these latter two groups, those without slip rings will weigh perhaps 25 to 35 per cent more than those with slip rings, all design factors being on an equivalent basis.

Another theoretical possibility which would fall in the last group is an inductor dynamotor with inductor-type booster generator. The practical advantages of such a combination is questionable.

The motor-generator set with inductor generator, class *A*3, has the advantages of ruggedness and reliability, since it has no conductors on the generator rotor and no slip rings. For 400-cycle service where light weight and hence high speed is required, the generator will probably be the twin-stator type with single stationary field coil in the shape of an an-

Figure 5. Centrifugally operated carbon-pile speed governor

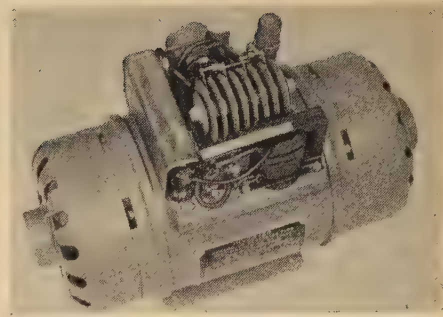


Figure 6. Physical arrangement of resonant-circuit feed-back speed-control equipment including capacitor, reactor, and rectifier shown in Figure 3

nular ring lying between the two stators. The generator rotor will have three salient poles or six effective poles on each end, for 400 cycles at 8,000 rpm. The direction of the flux is centrifugal in one stator and centripetal in the other, and crosses each air gap in three rotating bands of high and essentially constant density at the rotor pole faces.

The inductor dynamotor, class *C*, has a single armature with salient teeth or poles. The a-c winding lies in slots in the pole faces, the field poles otherwise being like those of an ordinary two pole d-c motor. This mechanism is essentially a merging of the motor-generator set with inductor generator into a single magnetic circuit. This combination does not produce a net gain necessarily, since control of output voltage (except in steps by a tap switch) is lost, and a speed of over 4,000 rpm for 400 cycles would result in difficulties. For some purposes however, such a machine has been extensively used with satisfactory results.

The cascade inverter consists of two parts in one frame and is physically similar to a two bearing motor-generator set. The d-c end acts as an inverted rotary converter built for 200 cycles, multiphase output. This output is delivered to the rotor of the a-c end, which is slotted and wound similar to the d-c armature, except that the 200-cycle phase rotation is reversed, as shown by Figure 2. Thus, the a-c end operates as an a-c excited generator, with combined electrically and mechanically rotating field. When built for a speed of 6,000 rpm for 400-cycle output, half of the energy is delivered from the d-c end electrically and half mechanically. There are four conventional stationary salient field poles on the d-c end, and a four-pole stator on the a-c end. The only control of output voltage possible is obtained by a tap switch connected to the output stator.

An inverted rotary converter with ro-

tating transformer, class $E2$, has been built, the advantages conceived being that the (circular three-legged) transformer would be well cooled; and six-phase low voltage could be carried directly to the transformer primary winding. This particular machine was intended for three-phase output only and hence had three slip rings.

The dynamotor-generator, class F, was designed to deliver both three-phase and single-phase output, and it was desired not to have the single-phase load unbalance the three-phase voltages. Accordingly, the motor was designed for operation at 8,000 rpm with six poles, and a dynamotor winding superimposed to deliver single-phase 400 cycles through slip rings. Figure 3 is the schematic diagram. The generator is a three-phase rotating-armature type. Hence this machine is a combination of class $A2$ and class B.

A dynamotor with booster, class G, has been built for three-phase output only. A dynamotor may be somewhat lighter than a motor-generator on account of having a single magnetic circuit, utilizing the same flux for both motor and generator conductors. A small generator adequate to supply the differential energy required to maintain constant output voltage is provided, acting as a booster. The greater part of the energy conversion takes place in the dynamotor. This was built in two and three kilovolt-ampere sizes and is probably the lightest type of inverter of the slip ring class for this particular duty.

A cascade inverter with booster, class H, has been built, which is a class D (cascade) inverter with tertiary element in the 200-cycle circuit between the d-c and a-c armature cores. This tertiary element is a small 200-cycle generator with four salient field poles and with each armature coil connected in series with the 24-wire 12-phase 200-cycle output from the d-c armature. The series boost effect at 200 cycles was added at an advanced phase angle so that with a lagging 400-cycle load the power factor of the 200-cycle load on the d-c armature might be brought near unity. The objective was to increase the efficiency since an inverted rotary converter operates most efficiently on a resistance load. However, the net over-all gain of this boosting at an advance angle is dubious, since the tertiary element must then have more capacity than would be the case for in-phase boosting.

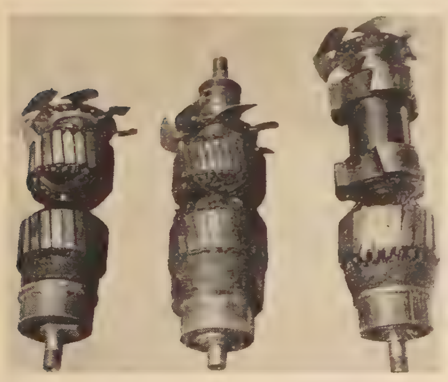


Figure 7 (left). Armature for cascade inverter

(Center). Armature for dynamotor-generator type. Twenty-six-volt a-c winding can be seen in the same slots with d-c motor armature winding

(Right). Rotating element including motor armature and rotor for inductor generator

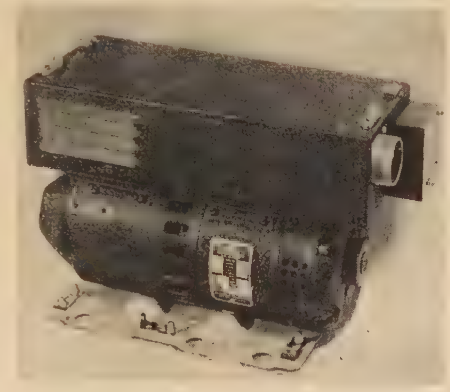


Figure 8. Inverter made by the Leland Electric Company

An inverter with inductor-type generator is being considered, with one stator having a three-phase winding and the other supplying the single-phase load.

At least four of the classes listed are in current use in large quantities.

As is frequently the case, there would be no particular problem involved if one type of machine had all of the advantages (minimum number of brushes, light weight, reliability, and independent sources of single- and three-phase output). Many practical considerations enter, and it may be that, even for a single well-defined set of load requirements, it will be difficult if not impossible to arrive at a universally accepted decision as to the best machine for the job.

The problem of obtaining satisfactory brush operation at high altitudes is now generally recognized in the electrical industry as a serious one. The approach to this, in so far as inverter design is concerned, has been to study the problem intensively in itself but simultaneously to develop inverter designs without generator slip rings. As indicated previously, a sacrifice in weight must be accepted in payment for the increased reliability of an inverter with no generator brushes.

Construction Details

Inverters for aircraft duty are made in two-bearing open-type frames. Specifications require openings to be small. Internal fans, either propeller or centrifugal type, are used. Sealed or labyrinth-type shielded bearings are generally used, with no provision for relubrication. A speed of 8,000 rpm is commonly used, but cascade types running at 6,000 rpm and inductor types at 4,000 are also widely used. The inverters must be capable of operating in any position, be built to withstand shock and vibration, and be capable of operating in any outdoor temperature and pressure encountered on the surface of the earth and up to 40,000 feet above it.

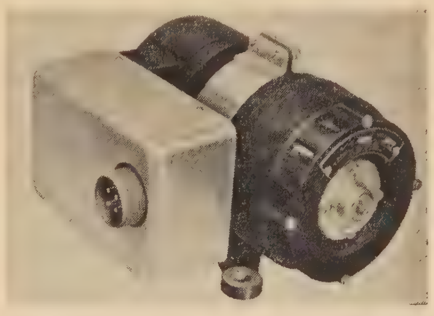


Figure 9. Inverter of Eclipse aviation division, Bendix Aviation Corporation

Hollow shafts are used in many of the inverters for weight reduction. End bells are made of magnesium or aluminum. Armature and stator laminations have holes or slots where all metal not definitely needed for carrying flux or for mechanical purposes is removed.

Armatures are, of course, dynamically balanced. In the case of some of these machines where the armatures are long, the shaft not too heavy, speed high and air gap small, special procedure may be required to insure that a static unbalance near the middle is not compensated by two weights added on the opposite side but near the bearings, resulting in a tendency for the shaft to spring.

With good air circulation easily obtainable at the high speeds used, and the losses kept relatively low to obtain good efficiency, it has not generally seemed necessary to provide for higher temperatures than commonly dealt with in other electrical apparatus.

Filters for suppression of radio interference are provided integral with the inverters.

Electrical connections are made with

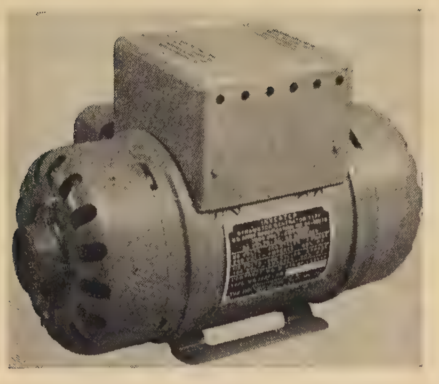


Figure 10. Holtzer-Cabot inverter

standardized *A-N* aeronautical-type fittings.

Frequency Control

Figure 4 shows a centrifugally operated contact-making speed governor which has no slip rings or brushes. The rotating element consists of a yoke carrying a spring which is deflected by the centrifugal force resulting from two weights. The axial motion of this spring is transmitted to contacts which are a part of the stationary unit by means of a "bakelite button" which engages with a dimple in the spring at the center of rotation. The points are of molybdenum-silver. The usual capacitor and resistor are provided across the contacts. The familiar Lee-type governor is also used.

A carbon-pile speed governor which is actuated by centrifugal force has been developed, the operation of which is superior to that of the contact-making governor in some respects. This is shown by Figure 5.

A resonant circuit feed-back speed-control circuit, Figures 3 and 6, is used on one inverter. This has a series circuit tuned for a peak at slightly over 400 cycles. In series with the capacitor and reactor in this circuit is a dry-disk rectifier. The d-c terminals of this in turn supply a control field on the motor. Consequently, as the output frequency approaches 400 cycles, the current in this control field rapidly increases, and the speed stabilizes. The rectifier delivers to the control field direct current plus an 800-cycle component. In order to maintain substantially constant flux, each field pole is provided with a heavy copper shading coil. Taps are provided on the reactor to compensate for manufacturing variables in the inverter and the capacitor.

Electronic controls have been developed for both speed and voltage. It would be quite possible to obtain extremely accurate frequency control by this means, if required, by using a tuning fork as the reference. Here again however, the greater the precision required the more susceptible to trouble the apparatus becomes—as well as being more bulky.

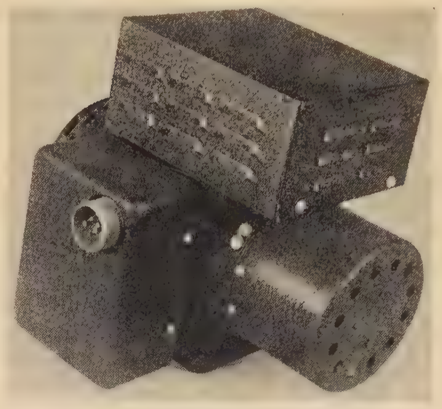


Figure 11. Inverter manufactured by General Electric Company

Voltage Controls

It is believed that no good purpose will be served by elaborating on theoretical possibilities for automatic voltage control. A long discussion could be presented, starting with the Tirrill-type regulator and leading up to electronic-type precision controls. These devices would include multifinger contact-type regulators, saturated reactors, and various others. The carbon-pile regulator, however, has come into very general use both for controlling the main d-c generators and for regulating the 400-cycle inverter output as well. Present indications are that it has no inherent serious disadvantages and that it produces satisfactory results. In applying a carbon pile to an inverter, over-all temperature compensation must be obtained, including the rectifier in the circuit supplying the actuating solenoid from the 400-cycle circuit being regulated.

Radio-Interference Filter

Specifications require the radio-frequency component of voltage in any input or output conductor of the inverter to be less than 50 microvolts at any frequency from 0.2 to 20 megacycles. For the purpose, it is common practice to use a π-circuit filter in each conductor. The reactance should not be so large as to cause excessive voltage drop in the 400-cycle conductors, and here air-core chokes are commonly used. Textbook rules for the most efficient design of choke result in minimum space and weight. Capacitors should be no larger than necessary, both because of space and weight considerations and the effect of the leading current on the no-load voltage where voltage regulators are not used. Great care must be used in designing a filter as to arrangement of components, length of leads, and method of grounding.

Conclusions

While continuous effort has been made to standardize ratings and performance requirements, this has been difficult. Load requirements change rapidly, and the way must be left open for refinements of inverter design.

A motor with automatic speed control driving a generator with automatic voltage control appears to be the most desirable general type for most purposes.

Production of existing designs which have proved to be usable must be continuously carried on; and simultaneously new designs must be studied and advantages and disadvantages weighed. Interchangeability must be considered. Also, designs cannot be changed too frequently on account of maintenance problems, spare parts for aircraft components now being carried at many points around the world.

Fundamental Principles of Amplidyne Applications

F. E. CREVER
ASSOCIATE AIEE

Synopsis: The Amplidyne generator has been widely applied and is recognized as an important device in the electrical industry. Since it is used primarily as a power amplifier in regulating systems, the requirements of an amplifier for this use are analyzed, and the characteristics of the Amplidyne generator are compared in this paper with these requirements.

Owing to the large number of distinct windings of the Amplidyne, it is desirable to follow a consistent practice in terminal markings and polarities. Proposals for this purpose are outlined in the paper, and typical values of amplification for different output ratings are given.

Service experience and an application typifying the use of the Amplidyne are discussed, so that the reader may become familiar with the device and its uses.

SINCE its introduction into the electrical industry, the Amplidyne generator has been found useful in a large variety of applications. The chief use has been in closed-cycle controllers or regulating systems as a power amplifier. In order to see why the Amplidyne is particularly adaptable and useful in regulating systems, it is desirable to review and analyze the characteristics of the regulating system to determine the requirements of an amplifier for use therein.

The Regulating System

A closed-cycle control system or regulating system is one in which the controlling agency is actuated by some function of the final output in such a manner as to minimize any deviation of the output from an ideal value.

There are three fundamental elements of such a system, namely:

1. A standard of ideal performance against which the output to be regulated can be compared.

2. An amplifier to amplify any deviation of output from the standard.

3. A means of feeding the amplifier output into the system in such a manner as to minimize the deviation.

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F. E. Crever, engineer, is with the General Electric Company, Schenectady, N. Y.

Many factors need to be considered in devising a regulating system, and these have been outlined in a previous article.[1] It is not the purpose here to elaborate on these factors any more than necessary to establish a basis for analysis of the amplifier requirements in the system and to show how such an analysis acts as a guide in evaluating various proposed amplifiers for this use.

The first element of the regulating system, namely, the standard of performance, may be either a primary or a secondary standard and must be of like physical kind to the quantity to be compared with it. It is very often necessary to transform the quantity to be regulated into the same form as the standard for comparison. It can be readily understood that standards and transformations can most readily and economically be obtained at low power levels.

The third element of the system, the means of feeding the amplifier output into the system, may, and in most cases does, require a very considerable amount of power.

It is obvious that a high degree of power amplification is an important requirement for an amplifier to be used in a regulating system.

Stability is another primary requirement. It is beyond the scope of this article to establish mathematically the requirements for stability of the regulating system. Instability can be explained by the existence of a series of time delays in the system. In general, the greater the number of time delays in series and the higher the amplification the greater will be the tendency to oscillate. Also, in a system with three or more simple exponential time delays in series, the more nearly the time delays equal each other in magnitude the greater will be the tendency to oscillate. Since in the usual industrial power apparatus the time delay of the final controlled element is appreciable, it is generally important to reduce to a minimum the time delays in the system prior to the final one. From these considerations, the second desirable characteristic of the amplifier is as short a time delay as possible between input and output.

One thing that has not been generally recognized is that the closed regulating circuit affects the response time of the system as well as minimizing the deviation between the standard and the system output. In this respect, a regulating system is very similar to an inverse feedback amplifier.

Analysis of Simple Regulating System

Figure 1 illustrates a very simple regulating system with one time delay. For purposes of analysis, assume that up to a time $t=0$ the standard voltage has been 0 and is instantly made equal to E_S at time $t=0$. Furthermore, assume the amplifier and generator amplifications to be linear over the range of voltage considered.

Let

R_1 = generator field resistance
L_1 = generator field inductance
K_1 = generator armature voltage per field ampere
M_1 = amplifier amplification

$$T_1=\frac{L_1}{R_1} \text{ generator field time constant}$$

$$E_1=-M(E_R-E_S)=(R_1+L_1p)i_1$$

$$E_R=Ki_1$$

$$-ME_R+ME_S=(R_1+L_1p)\frac{E_R}{K_1}$$

$$\frac{E_R}{K_1}R_1(1+T_1p)+ME_R=ME_S$$

$$E_R=\frac{K_1ME_S}{R_1\left(\dfrac{MK_1}{R_1}+1+T_1p\right)}$$

Let

$$\alpha=\frac{K_1M}{R_1}=\text{over-all system amplification}$$

Then

$$E_R=\frac{\alpha E_S}{\alpha+1}\left(1-\epsilon^{-\frac{(\alpha+1)t}{T_1}}\right)$$

If we had merely applied a voltage E_1 to the field of the generator sufficient to bring the output voltage without the regulating system up to a value equal to E_R, the equation for E_R would have been

$$E_R=\frac{E_1{}^1K_1}{R_1}\left(1-\epsilon^{-\frac{1}{T_1}t}\right)$$

Thus, we see that the use of the regulating system not only minimizes deviation from the value of the standard but has reduced the time delay of voltage build-up or effectively decreased the time constant in the ratio of $\alpha+1$ to one.

This analysis assumes linearity of amplification, and it is to be noted that if the voltage output of the amplifier is limited,

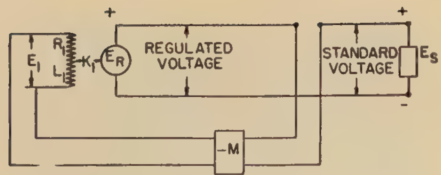


Figure 1. Simple regulating system

the response will be determined by the maximum voltage available at the generator field.

The Amplidyne

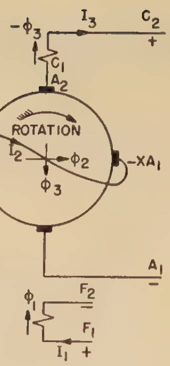
The Amplidyne has been described in detail in other articles.[2–4] It is here desired to point out the features of its design which make it outstanding in its performance.

A review of the operation is desirable to this end. Figure 2 shows a two-pole d-c armature with two pairs of brushes in quadrature. The frame is omitted from the diagram for simplicity, and the assumption is made that commutation is ideal. A flow of current I_1 in the control field F_1F_2 in the load axis creates a load-axis flux ϕ_1. The armature conductors cut this flux, and a voltage E_2 is generated in them to send current I_2 through the brushes of the short-circuit axis. The flow of armature current I_2 through the short-circuit axis of the armature develops a distributed magnetomotive force and flux ϕ_2 in the short-circuit axis. Rotation of the armature in this field generates a voltage E_3 at the load-axis brushes. The magnetomotive force of the armature caused by current I_3 is in opposition to the magnetomotive force of the load-axis control field. However, there is a distributed compensating field which neutralizes the armature magnetomotive force caused by current I_3. Only a very small magnetomotive force is required in the load-axis control field to cause generation of the required voltage and current in the short-circuit axis to give full output voltage at the load terminals. Only a small generated voltage is necessary in the short-circuit axis, and yet the full voltage of the armature is available for forcing a rapid rise or decay of flux ϕ_2.

Analogy and Analysis of Time of Build-Up

The Amplidyne combines into one machine two stages of amplification, each stage of which has a very small time delay. The first stage of amplification is from the load-axis control field to the short-circuit axis, and the second stage is from the short-circuit axis to the load terminals. For purposes of approximate

Figure 2. Schematic diagram illustrating operation of the Amplidyne



analysis, it is as though two d-c generators were in series and with the first generator just as large as the second as regards voltage output capability. This is illustrated in Figure 3.

Let a unit voltage E_1 be suddenly applied to the control field.

Let $T_1=\dfrac{L_1}{R_1}K_1=$ volts E_2 generated per field ampere i_1

$K_2=$ volts E_3 generated per field ampere i_2

$T_2=\dfrac{L_2}{R_2}$

Then

$$E_1=(R_1+L_1p)i_1$$

$$E_2=K_1i_1=\frac{K_1E_1}{R_1+L_1p}=(R_2+L_2p)i_2$$

$$E_3=K_2i_2=\frac{K_1K_2E_1}{(R_1+L_1p)(R_2+L_2p)}$$

$$E_3=\frac{K_1K_2E_1}{R_1R_2(1+T_1p)(1+T_2p)}$$

The solution of this is

$$E_3=\frac{K_1K_2E_1}{R_1R_2}\left[1+\frac{T_1}{T_2-T_1}\epsilon^{-\frac{1}{T_1}t}-\frac{T_2}{T_2-T_1}\epsilon^{-\frac{1}{T_2}t}\right]$$

Figure 4 is a plot illustrating the voltage rise in such a circuit having constants as follows:

$T_1=0.058$ second $\quad T_2=0.092$ second

These are values taken from a typical 1,500-watt 250-volt 1,750 rpm Amplidyne. The time delay T_1 is for a field occupying 25 per cent of the field coil space and capable of carrying continuously ten times the current required to excite the machine to 80 per cent voltage at

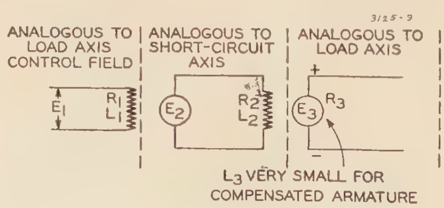


Figure 3. Two-machine equivalent of the Amplidyne

rated load. The time delay T_2 is on a machine with a series field in the short-circuit axis which is connected in the armature circuit of the short-circuit axis.

From this analysis, it is evident that the Amplidyne is an ingenious method of obtaining two-stage amplification with a fast response in a single unit. A number of independent load-axis control field windings can be used, so that the resultant control ampere turns will represent the algebraic sum of several independently selected functions.

Circuit Diagrams

To take full advantage of these possibilities and to avoid confusion in circuit diagrams, the symbols shown in Figure 5 have been found convenient. These symbols may be used as a guide for any combination of field windings. Connections of typical machines are shown in the diagram, and it can be seen that they are relatively simple. The only external connections are those of the various load-axis control fields and the power output leads.

Terminal Letters and Nomenclature

The same general rules are used for designating Amplidyne terminals as those in use for other d-c machines. The prefix X is added to the terminal markings of all windings which magnetize in the short-circuit axis, while the windings which magnetize in the load axis are marked without a prefix. See Figure 5 for detailed definitions of the various symbols.

Throughout the paper, the terms *load*

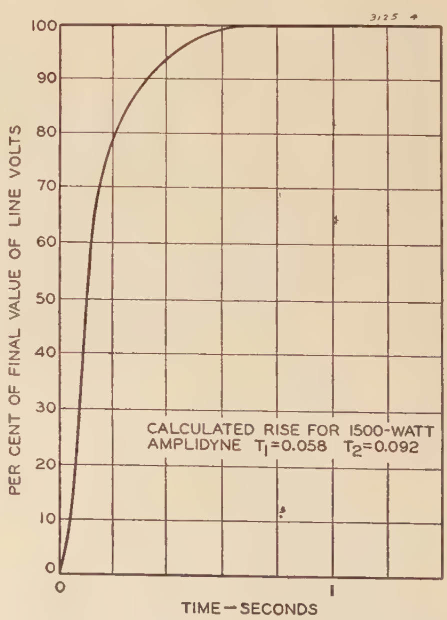


Figure 4. Response curve of typical Amplidyne generator

axis and *short-circuit* axis are used to describe the two axes. Other publications have also referred to the *load axis* as the *secondary axis* and the *direct axis*, and to the *short-circuit axis* as the *primary axis* and the *quadrature axis*.

Polarity

Amplidynes are so marked that for current which flows from the lower to the higher subscript terminal, in any of the control fields which magnetize in the load axis, the polarity of the terminal A_1 of the armature winding of the load axis is negative. This is true for both directions of rotation. Refer to Figure 5 for a detailed explanation of various windings.

Methods of Rating

Amplidyne generators are rated in kilowatts output, the product of rated voltage and rated current. It has been found desirable to select the voltage rating so that the saturation curve is essentially a straight line up to 80 per cent of rated voltage, and to provide a ceiling voltage approximately equal to 120 per cent of rated voltage. Typical saturation curves are shown in Figure 6. Usually, amplidyne generators are designed for 40 degrees centigrade temperature rise at rated voltage and current and to withstand a 200 per cent current peak momentarily at rated voltage. The machines should preferably be subjected to not

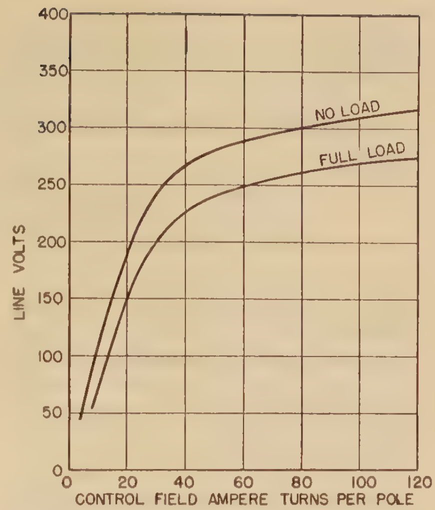


Figure 6. Typical saturation curves for 1,725 rpm 250-volt Amplidyne

more than 150 per cent rated current at rated voltage, tapering to 250 per cent at zero voltage.

The control field excitation requirements for typical 1,800-rpm Amplidyne-generator ratings between $^1/_2$ and 25 kw vary from 0.25- to 0.75-watt excitation (in one-fourth field space) with rated current output and 80 per cent rated volts; giving power amplifications (for one-fourth field space) from 2,500 to 40,000

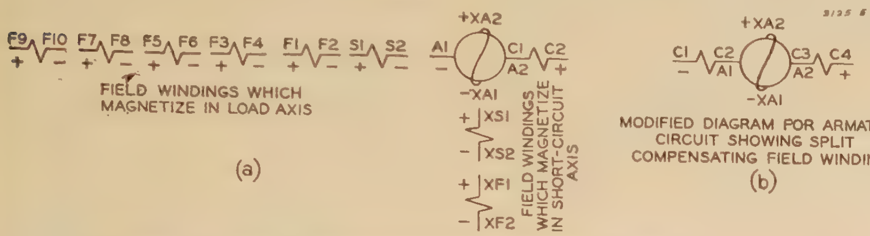


Figure 5. General diagram for clockwise rotation of Amplidyne generators

1. Compensating field windings
The compensating field windings, C_1C_2, are always connected in series with the load axis of the armature winding. Frequently it is convenient to split the compensating field winding so that half is connected in the circuit on each side of the load axis of the armature winding, and the diagram is modified as shown in diagram a

2. Commutating field windings
If a commutating field winding is used it will be connected in the armature circuit between terminals $A1$-$A2$ or $XA1$-$XA2$

3. Field windings in the load axis
Each terminal of the field windings in the load axis is marked with a single letter and subscript number. Exciting current which flows from the lower to the higher numbered subscript in any one of these windings produces a magnetomotive force and flux along the load axis and a speed voltage in the armature winding of the short-circuit axis of such polarity as to make XA_2 positive and XA_1 negative with clockwise rotation of the armature.
The use of the letters and subscripts has additional significance:
S_1S_2, S_3S_4, and so forth, field windings intended for series connection; usually in the armature circuit of either the load axis or the short-circuit axis
F_1F_2, main control shunt field
F_3F_4, reference or counter shunt field
F_5F_6, auxiliary control shunt field
F_7F_8, antihunt shunt field
F_9, F_{10}, F_{11}, F_{12}, and so forth, shunt fields for other purposes than those already noted

4. Field windings in the short-circuit axis
The terminals of the field windings in the short-circuit axis are marked the same as the field windings in the load axis except that the prefix letter X is added. Exciting current which flows from the lower to the higher numbered subscript in any one of these windings produces a magnetomotive force and flux along the short-circuit axis and a speed voltage in the armature winding of the load axis of such polarity as to make A_2 positive and A_1 negative with clockwise rotation of the armature
The use of the letters and subscripts has added significance:
XS_1XS_2, and so forth, field windings intended for series connection; usually in the armature circuit of either the short-circuit axis or the load axis
XF_1XF_2, and so forth, field windings intended for shunt connection

5. Killer windings
A killer winding is sometimes used in special cases to reduce the residual. This may be a special winding or it may be special taps on a winding designed for other purposes. In either event the terminals are marked K_1K_2. Since the exciting current is alternating, the polarity marking is omitted

6. Counterclockwise rotation
The same letters and subscripts are used for both rotations. For counterclockwise rotation the polarities of all field windings and of the armature winding of the load axis remain the same as for clockwise rotation. The polarities of all field windings and the armature winding of the short-circuit axis are reversed. All interconnections of the various windings are the same for both rotations; except that field windings in the load axis which are connected in the circuit of the short-circuit axis, and field windings in the short-circuit axis which are connected in the circuit of the load axis must be reversed when rotation is reversed

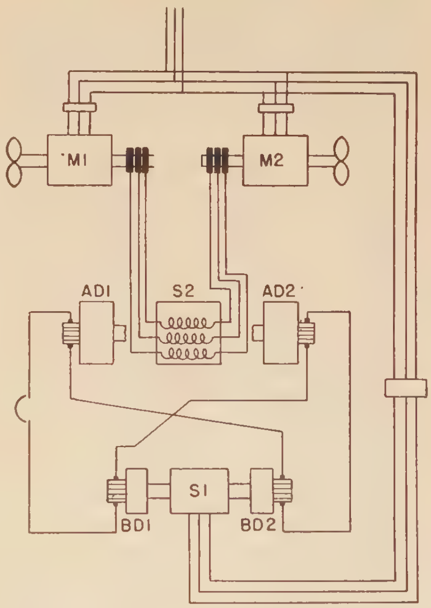


Figure 7. Schematic connection diagram with double motor drive using counterrevolving propeller

$M1$ and $M2$ = 22,000-horsepower slip ring 327-rpm fan motors
$AD1$ and $AD2$ = 2,500-kw d-c generators
$BD1$ and $BD2$ = 3,200-horsepower d-c motors
$S2$ = 20,000-kva 540-rpm synchronous motor
$S1$ = 6,000-kva 514-rpm synchronous generator

to one. The larger values of excitation and amplification apply to the larger ratings.

Power amplification and rate of response are interrelated; that is, for a particular design an increase in power amplification will normally result in a reduction of the rate of response, and an increase in the rate of response will normally result in a reduction in power amplification.

Applications and Service Experience

There have been a great many applications of Amplidyne generators in regulating systems, some of which have now been in service from four to five years. Some typical applications were given in a previous article.[1]

The service record of these machines and their circuits has been excellent.

Typical of the type of regulation that can be accomplished is the application of Amplidynes to wind tunnel drives. The arrangement of a wind-tunnel-drive apparatus was described in a previous article.[5] Figure 7 shows the schematic connections of the main drive. In these circuits, the following functions were performed with Amplidynes and associated circuits.

1. Variation of field current of constant speed and variable speed d-c machines—*BD*1, *BD*2, *AD*1, and *AD*2 to

(*a*). Controlled acceleration of the variable speed machines from rest to full speed and matching of the line frequency for synchronizing.

(*b*). Controlled acceleration of the main fan motors from rest

(*c*). Maintain fan motor speed within one-fourth of one per cent.

(*d*). Under any condition impose a definite current limit in the circuit between the d-c machines.

2. Variation of field current of variable speed a-c generator S_2 to

(*a*). Match voltages during the synchronizing period.

(*b*). Maintain system power factor during operation of the tunnel.

Owing to the fact that these circuits involve matching the quantity to be regulated against a standard, it is essential that the proper polarities be predetermined and checked before the apparatus is given full power to regulate. In case of error of connections, the system might cause the value being regulated to depart from the standard with increasing voltage rather than bringing about the desired matching.

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Guide for Wartime Conductor Temperatures for Power Cables in Service

AEIC SUBCOMMITTEE ON
WARTIME TEMPERATURES FOR CABLE CIRCUITS

Preface: The cable engineering section of the Association of Edison Illuminating Companies recently sponsored a guide particularly for the use of its member power-utility companies in the wartime operation of electric cables of the types as used mainly in underground systems. A large share of the underground cable circuits in this country are operated by the AEIC companies. The details of preparing the guide for the consideration, revision, and approval of the section were handled by a special subcommittee.

The AIEE cable working group sponsored a conference on emergency rating of power cables at the AIEE technical meeting on January 26, 1943, at which nine papers were presented.* Subsequently it seemed desirable to the cable working group to have the AEIC guide also presented for the purpose of giving information to all interested in this country and for stimulating discussion. The information and diversified views in the nine papers were considered in preparing the guide.

This guide is similar in two respects to wartime guides recently prepared by various AIEE committees and subcommittees on various kinds of equipment, namely:

(*a*). It presents principles and assumptions concerning the factors affecting the operation and life of the equipment involved, thereby indicating how to obtain maximum utilization within the prescribed temperature limits.

(*b*). It presents tables of temperatures for occasional emergency use which are above the limits in the AIEE Standards.

Furthermore, this Guide has tables of temperatures for normal operation, that is, day-in and day-out operation, where the temperatures are higher than the limits given in various specifications and standards, including the AIEE Standards. The usage of temperatures such as set forth in the guide for normal operation and for emergency operation will produce sharp increases in rates of deterioration and in rates of failures of cable circuits. Such temperatures are recommended for consideration only as a wartime measure to save critical materials, because their use is otherwise uneconomical as well as damaging to service reliability.

* A single pamphlet containing all nine papers is available in limited quantity at Institute headquarters at 75 cents a copy.

1. Scope

THIS guide has been prepared as an aid in determining reasonable cable operating temperatures for wartime service for both normal and emergency operating conditions. The aim has been to obtain the maximum practical utilization of materials and facilities consistent with maintaining tolerable rates of failure. Tables are presented showing recommended maximum temperatures for representative systems, but the values must be adjusted, in many cases, to take into account the various special conditions that are involved in specific situations. As an aid to the engineer who must make the decision as to the needed adjustments, a large part of the guide is devoted to a discussion of the principles on which the recommendations are based and to suggestions as to the adjustments appropriate for particular cases.

The recommendations cover four types of power cable used mainly in underground systems, namely:

1. Impregnated-paper-insulated lead-covered cable of the solid type.

2. Impregnated-paper-insulated lead-covered cable of the oil-filled type.

3. Varnished-cambric-insulated lead-covered cable.

Paper 43-104, recommended by the AIEE committee on power transmission and distribution for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted April 21, 1943; made available for printing May 20, 1943.

Personnel of the AEIC subcommittee: Herman Halperin, *chairman;* C. T. Hatcher, and H. W. Collins.

Personnel of the AEIC cable engineering section: W. F. Davidson, *chairman;* G. M. Armbrust, T. J. Brosnan, W. R. Bullard, H. A. Clark, M. T. Crawford, G. E. Dean, R. L. Dodd, F. M. Farmer, C. W. Franklin, T. H. Haines, Herman Halperin, L. I. Komives, C. H. Kraft, S. J. Lisberger, R. E. Morse, H. S. Phelps, F. E. Pinckard, and C. T. Sinclair.

4. Rubber-insulated cable with and without lead sheath.

This guide is not intended to supersede existing standards and recommended practices for operating temperatures under peacetime conditions of service. Further, it is recognized that if these higher temperatures are used the manufacturer cannot be held responsible under the usual guarantees in standards and specifications for troubles resulting from operation at the increased temperatures.

2. General Principles

Under wartime conditions, the normal balance between many factors that enter into the determination of proper cable operating temperatures is upset. Maintenance of service remains important, but the very high standards of peacetime service may not be justified; economic factors are assigned only small consideration. As a general matter it may be assumed that underground cable systems can operate with failure or trouble rates considerably higher than those reported in the cable operation reports of the Edison Electric Institute without materially affecting the reliability of service to the customers. Some increase in maintenance cost is to be expected, but this is partially offset by the increased use of the investment. Some reduction in life is to be expected; the extent of this is indicated in the assumptions.

Establishment of increased temperatures and increased load ratings under these changed conditions requires a large measure of engineering experience and judgment. However, it is practical to make recommendations as to temperatures for wartime conditions for representative systems and to give sufficient discussion for use in making adjustments in the temperatures as dictated by circumstances peculiar to specific systems.

2.1 Type of System

The recommended temperatures given in Tables I, II, and III apply to representative systems meeting three general conditions. These are:

(*a*). The cable in service was purchased almost entirely under the following standards and specifications: "AEIC Specifications for Impregnated Paper-Insulated, Lead-Covered Cable, Solid Type" (the "Simplified Practice Schedule" issued in 1941 not being included); "AEIC Specifications for Impregnated Paper-Insulated Lead-Covered Cable, Oil-Filled Type"; "AIEE Standard Number 30," issued at various times; "Insulated Power Cable Engineers Association Specifications for Varnished Cambric-Insulated Cables" (the 1939 edition which applies to the recently developed varnished-cambric tapes not being included); the "National Electrical Code," and "ASTM Standard Number 469" for rubber-insulated cable.

(*b*). The system had rates of trouble of the same general order as the national rates of trouble as given in the "Cable Operation Reports" of the Edison Electric Institute. The cable and accessories have no especially bad condition that would lead to a very large increase in troubles with the use of higher temperatures.

(*c*). The power cable systems involved will have reasonably good maintenance, which includes periodic inspections in manholes and repairs when found necessary.

2.2 Temperatures—Definitions

2.21 The temperatures referred to are those at the hottest portion of the cable in a given line or circuit. Where these limiting temperatures exist in only a small portion of the line, attention should, obviously, be given to a determination of the extent to which corrective measures may be justified. (Section 3.8)

2.22 The "normal" temperatures are the maximum copper temperatures that may be reached in regular daily operation during wartime when all of the lines or circuits for a given supply are in service. Where, for example, the daily load cycles for the first five days of the week are practically the same and then the load drops considerably over the week end, then the suggested maximum allowable normal temperature would apply to the conditions that produced the maximum temperature during the week.

2.23 The "emergency" temperatures apply to special operation occurring not more than four periods in any 12 consecutive months for all of the types of cable except the oil-filled type, for which the limiting number of periods is two in any 12 consecutive months. The duration of the special operation is assumed to be a continuous period of time of 24 hours or less for all the types of cable except for the oil-filled type, for which it is assumed that each period is 60 hours or less.

2.24 It is assumed that, both in normal and in emergency operation, the cable is at approximately the maximum temperatures for a few hours or more in each daily lead cycle, which is practically the condition when lines or circuits have their daily heavy-load periods for eight or twelve hours or more.

2.3 Recommended Temperatures

2.31 For impregnated-paper-insulated cables of the solid type the temperatures for normal operation are from 4 to 15 degrees and for emergency operation from 10 to 40 degrees centigrade higher than the temperature limits given in the AEIC Specifications and the AIEE Standards. Lower temperatures are shown in Table I for shielded three-conductor cables than for single-conductor cables, because the maximum temperatures for three-conductor shielded cables are more limited by the effect of cable movement upon the sheath life than by deterioration of the insulation.

2.32 For oil-filled cables the temperatures for normal operation are ten degrees centigrade above the limits given in the AEIC Specifications except for voltages below 26 kv where the increases are smaller, and the proposed emergency temperatures are from 25 to 30 degrees centigrade above the AEIC limits for emergency operation except for voltages below 26 kv.

2.33 For varnished-cambric cables the proposed normal temperatures are from 8 to 14 degrees centigrade above the limits given in AIEE Standard 30, and the proposed emergency temperatures are from 23 to 31 degrees centigrade above those limits.

2.34 For rubber-insulated cables except those with ozone-resistant compounds, the proposed normal temperatures are from 5 to 20 degrees above the limits given in the 1940 National Electrical Code and the proposed emergency temperatures are from 15 to 40 degrees centigrade above those limits. No standard limits are available for ozone-resistant compounds, but general values from manufacturers have been about the same as given in the table.

2.4 Assumed Influence on Cable Life

Higher temperatures result in reduction in cable life. Three assumptions have been made as a basis for the recommendations.

2.41 It is assumed that the wartime "normal" temperatures will at least double the rate of deterioration of the cable as compared with the rate for previous practices based on "normal" temperatures as given in existing standards and specifications. The increase in failure rate will be one indication of an increase in rate of deterioration, but it is to be noted that generally this may not become apparent in less than six months or a year.

2.42 It is assumed that wartime "emergency" temperatures would cause a loss of life, for each period they are used, of less than one per cent of the expected life of the cable and accessories when they were new, and that they would cause no immediate failures.

2.43 The increases in the rate of deterioration for normal operation and in

the loss of life of one per cent or less for emergency operation are meant to apply to either the insulation or the sheath as the case may be. It should be recognized that in many cases cable systems have operated at temperatures considerably below the limits given in national standards. In such cases the use of the recommended temperatures in this guide and their accompanying increased temperature ranges may cause increases of 200 or 400 per cent or even more in the rates of deterioration over the rates that previously obtained. The suggested maximum allowable temperatures for wartime operation are based on a large amount of experimental data and operating experience. "Special Considerations" discusses this at some length for a number of cases, while there is more extensive discussion in several papers listed in the bibliography.

3. Special Considerations

3.1 Influence of Operating Voltage

Temperatures are given in the tables for various nominal operating voltages and were determined by the formulas shown. For other nominal operating voltages, the suggested allowable temperatures can be determined by interpolation from the temperatures in the tables or by the use of the formulas.

3.2 Lead Sheath

3.21 For normal temperatures which apply to day-in and day-out operation, one of the main factors affecting the determination of the temperature for lead-sheathed cable is the effect of daily movement of the cable in the manhole incidental to cyclic loading. Such movements may lead to cracking through the sheath before the insulation becomes unserviceable. The temperatures given in the tables are based on the assumption that the daily load varies considerably through each 24-hour period and there is one general movement of the cable in and out of the duct in each 24 hours. If the loading is such as to have two general cycles of movement back and forth, then the temperatures should be reduced in order to avoid considerable cracking through the sheaths in the manholes. On the other hand, if the loading is fairly steady through each 24-hour period, then this factor may permit the use of higher normal temperatures.

3.22 In connection with sheath cracking in manholes incidental to daily cyclic loading, another factor is the training of the cable in the manhole as well as the size of manhole. Where the offset between the center line of the duct and the center line of the line is small, or the bending radii of the cable are small, or the total length between joint wipe and duct mouth is small, then the effect of a given movement of the cable at the duct mouth in causing sheath cracks is increased. Where these characteristics are particularly below what may be considered good practice, then there should be a corresponding reduction made in the allowable temperatures, especially where the daily temperature ranges are large.

3.23 The amount of cable movement at the duct mouth is affected by the length of cable between manholes, within limits. For short lengths of cable, such as 100 or 200 feet, the allowable copper temperatures might be set somewhat higher than given in the tables as far as considerations of the lead sheath are concerned.

3.24 For a given length of cable and temperature range, the amount of movement at the duct mouth is somewhat proportional to the ratio of the total cross section of the copper conductors in the cable to the weight of the cable. For a cable having relatively small conductors, the temperatures might be set at somewhat higher values than given in the tables.

3.25 The strain of the sheath in the manhole incidental to a given movement at the duct mouth is somewhat proportional to the diameter of the cable. Where the cable diameters are considerably more than about one and one half or two inches for low-voltage cables or about two or three inches for high-voltage cables, then the maximum temperatures for normal daily loading might well be less than given in the tables. Where the cable diameters are particularly small, then higher temperatures than given in the tables may be permissible for normal operation.

3.26 Impregnated - paper - insulated cable installed vertically or on steep slopes may be subject to excessive sheath expansion from the internal hydrostatic pressure. The expansion of the lead sheath is accelerated by the rise in temperature of the sheath. For practical purposes it is well to assume that the rate of creep doubles for an increase of about 17 degrees centigrade.

3.27 Where solid-type cables are operated with reservoirs under positive pressure connected to joints, consideration must be given when increasing operating temperatures to the possibility of increased sheath expansion and an increased rate of sheath bursting. Reduction of reservoir pressure may be considered as a compensating measure.

3.28 In locations where corrosion is a problem, increased sheath temperatures may increase the rate of chemical reaction and failures resulting from corrosion.

3.3 Conduit

3.31 Another important factor related to the use of higher temperatures for normal operation is the effect of the accompanying increase in conduit temperatures. In some cases the use of the increased temperatures will result in drying out the conduit and surrounding soil with a resultant increase in the heating constant, that is, the thermal resistance between the cable and base ambient, which is unheated earth some distance from the conduit for underground installations. As regards temperatures for emergency operation, the main point on the heating of the conduit is the fact that the tem-

Table I. Maximum Conductor Temperatures for Impregnated Paper-Insulated Lead-Covered Cable for Wartime

Caution: Values in tables are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts (Phase-to-Phase)	Maximum Conductor Temperature—Degrees Centigrade: Normal Operation	Emergency Operation
Single-Conductor Cable, Solid Type		
0–1	100	125
4	98	121
13	92	113
22	87	105
27	84	100
33	81	95
44	75	84
66	64	70
Three-Conductor Shielded Cable, Solid Type		
13	87	108
22	82	100
27	79	95
33	76	90
Multiple-Conductor Belted Cable, Solid Type		
0–1	98	125
4	94	115
13	85	104
22	76	94
27	71	88
33	65	80
Oil-Filled Type of Cable		
35 and less	85	115
66	85	110
132	80	105

Rules for establishing temperatures shown were as follows:

Type of Cable	Normal Operation	Emergency Operation
Single-conductor, solid	$100-E$(100 degrees centigrade for 0–1 kv and minimum of 64 degrees centigrade)	$125-1.6E$(125 degrees centigrade for 0–1 kv and minimum of 70 degrees centigrade)
Three-conductor shielded, solid	$95-E$	$120-1.6\ E$
Multiple-conductor, belted, solid	$98-E$ (98 degrees centigrade for 0–1 kv)	$120-1.2E$(125 degrees centigrade for 0–1 kv)

E is kilovolts between phase and ground for single-conductor and three-conductor shielded cables and between phases for multiple-conductor belted cables.

perature of the conduit may go up a few degrees for each 24 hours of emergency loading.

3.32 In all calculations as to carrying capacities for a given installation or installations of cable circuits, certain assumptions are made as to the effect of the heating of the cables in raising the temperature of the conduit or air surrounding the cable. If the assumptions are based on factual data obtained by surveys of the conduits and the installations are further followed up by surveys after the circuits are in operation, then the copper temperatures can be calculated with a good degree of accuracy. In such cases the load ratings may be determined without making especially liberal allowances for the temperature rise of the duct or other ambient over the base ambient. If surveys of conduits are made where the cables are operating at relatively high temperatures, then the load ratings can be made somewhat higher than would be the case if no surveys are made. If no factual data are available on the heating characteristics of the medium in which the cable is to be installed and no surveys are to be made, then the temperature that is used in calculating the load ratings might well be five or ten degrees centigrade less than given in the tables. On the other hand, where complete data are available and field surveys are continually made, it may be feasible to use temperatures of a few degrees up to ten degrees centigrade above the limits given in the tables for paper-insulated cables, the larger increases applying to the lower-voltage cables.

3.33 The total carrying capacity of all the cables in a conduit, expressed in kilovolt-amperes, may be increased in some cases by relocating some of the relatively lower-voltage cables into other conduits. Although the increase in current-carrying capacity for the higher-voltage cables might be small, the increase in kilovolt-amperes might be rather large.

3.4 Insulation

3.41 For, particularly, impregnated-paper-insulated cable made prior to 1920 and having high dielectric losses, and for varnished-cambric-insulated cable used at about nine kv and higher, the existence of large variations in dielectric loss, especially at the higher temperatures, should be recognized in calculating the carrying capacities. Where the information on dielectric losses throughout the expected temperature range of operation for a particular kind of cable is not available, then reasonably large allowances should be made for the dielectric losses or the temperature limits used should be somewhat less than given in the tables for both normal operation and emergency operation. Special attention should be given to the possibility of failures resulting from thermal instability for old cables with high dielectric losses.

3.42 Another effect of the temperature range is in the production of ionization in the solid-type paper-insulated cable. For old belted-type cable such as made in the period of 1920–30 with somewhat less insulation than used prior to 1920, the effect of ionization in normal operation may be considerable in some instances. If the operating experiences have indicated this to be the case, a small reduction below the temperatures given in the table for normal operation should be made, and a somewhat larger reduction should be made in temperatures for emergency operation.

3.43 Cable that has operated at temperatures corresponding to voltages much below its rated voltage may become unfit so far as the insulation is concerned to operate subsequently at rated voltage.

3.44 The insulation of recently made paper-insulated cable of the solid type can withstand somewhat higher temperatures for emergency operation than given in the table.

3.45 Varnished - cambric - insulated cable made in the past few years can withstand temperatures roughly five degrees centigrade higher than given in the table for both normal and emergency operation.

3.46 It should be noted that the resistance to effects of heat by rubber compounds of a given class has been found to vary considerably. If test or operating data are available for a given installation, the data may be used to modify the temperatures in the guide somewhat up or down.

3.47 Deterioration of rubber is greatly accelerated by oxidation. As a consequence, higher permissible temperatures are shown in Table III for lead-sheathed cable than for cables without lead sheath, except for ozone-resistant compounds, where the maximum temperatures are limited by softening of the insulation. Where the ends of lead-sheathed cables are of such construction as to allow air to be more or less in free contact with the rubber insulation, then, excepting ozone-resistant compounds, somewhat lower temperatures than given in the table may be advisable.

3.5 Accessories

3.51 In some instances the loading of the cables in emergencies to the temperatures given in the tables will result in movements at the duct mouth of one and one half or two and one half inches or even more. Training of the cable in the manhole should be such as to permit these large movements without buckling of the cable or jamming of the joint against the manhole wall or other joints or undue mechanical strains on connections to the joints to cause serious damage. If this is not feasible, then lower temperatures than given in the tables for emergency operation are suggested.

3.52 In some cases joints and potheads may develop troubles if operated at the emergency conductor temperatures given in the tables. This applies particularly to the higher-voltage cables. In general, however, the accessories on an underground system operate with ambients 15 or 25 degrees centigrade below the maximum duct temperature along the circuit at the time of emergency operation, and so the possibility of trouble with the accessories in such instances is thereby reduced or eliminated. The point, however, is of significance where the cable and accessories are all exposed to the same ambient, as might be the case with an open installation of a circuit in a generating station.

3.53 Where joints are filled with viscous or hard compound, increased tempera-

Table II. Maximum Conductor Temperatures for Varnished-Cambric-Insulated Lead-Covered Cable for Wartime

Caution: Values in table are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts (Phase-to-Phase)	Maximum Conductor Temperatures—Degrees Centigrade	
	Normal Operation	Emergency Operation
Three-Conductor Shielded and Single-Conductor Cable		
0–1	88	105
4	85	102
13	77	94
22	70	86
27	70	82
Multiple-Conductor Nonshielded Cable		
0–1	88	105
4	82	99
13	70	85

Rules for establishing temperatures shown were as follows:

Normal Operation	Emergency Operation
$88 - 1.5\,E$ (88 degrees centigrade for 0–1 kv and minimum of 70 degrees centigrade)	$105 - 1.5\,E$ (105 degrees centigrade for 0–1 kv)

E is kilovolts between phase and ground for single-conductor and three-conductor shielded cables and between phases for multiple-conductor nonshielded cables.

Table III. Maximum Conductor Temperatures for Rubber-Insulated Cable in Wartime

Caution: Values in table are not to be used without full consideration of limitations discussed in text

Nominal Operating Voltage—Kilovolts	Maximum Conductor Temperature—Degrees Centigrade: Normal Operation, No Lead Sheath	Normal Operation, Lead-Sheathed	Emergency Operation, No Lead Sheath	Emergency Operation, Lead-Sheathed
Code and Intermediate Compounds				
0–1	60**	70*	70	90*
30 Per Cent Class *AO*, Performance, and Other Similar Compounds				
0–7.5	65	75*	75	95*
Ozone-Resistant Compounds (Oil Base)				
0–7.5	75	75	85	85
7.6–15	70	70	80	80

* Terminal ends of cable assumed to be sealed against entrance of air.

** These temperatures are based on the 1940 National Electrical Code, which gave 50 degrees centigrade for code rubber together with Interim Revision Number 41 dated February 26, 1942, which permitted the use of the higher current ratings given in the previous editions of the code.

tures may cause trouble because of bulging of the casing or because of repeated bulging and collapsing of the casing.

3.54 Reservoirs may be subjected to volume expansion to beyond their limits when cables are operated at some of the higher temperatures given for oil-filled cables, and for those installations of solid-type cable having oil-filled reservoirs connected to the oil-filled joints. Therefore, special provisions should be made to take care of extra expansion of the impregnating compound or oil of the cable where the present reservoirs are inadequate.

3.55 Consideration must be given for oil-filled cable installations to the fact that dropping of heavier loads corresponding to higher temperature limits will result in hydrostatic pressure drops in the cable system greater than contemplated in the original design of the oil supply system. Provision must be made to prevent pressures below atmospheric forming at any point of the line under these conditions.

3.6 Load Cycles

As indicated under "General Principles" the recommended temperatures are based on the assumption that the cable may operate at about these temperatures for a few hours or more in a day. The heavy portion of the load of a given day may be of such short duration that no stable temperature conditions are attained and the maximum temperature is reached only momentarily. In such an event, if the temperature before the time of heavy load is considerably below the permissible temperature in the tables, then the maximum temperature could be five or ten degrees centigrade above the value in the tables, the higher temperatures applying to the lower voltages. This statement applies to temperatures during normal or emergency operation as the case may be.

3.7 Inspection

It is recommended that when lines or circuits are operated at temperatures such as given in the tables, the cable systems be given increased inspection. In the case of emergency operation the inspection should be made with little delay.

3.8 Special Measures

As previously indicated, the utilization of lines and circuits already in service can be increased within the allowable temperature limits in this guide through certain practices and arrangements, some of which are listed below:

1. Temperature surveys.

2. Increased inspection and maintenance program.

3. Changing solid bonding to special bonding for single-conductor cables to eliminate sheath losses.

4. Forced cooling by water or air during special temperature peaks at especially hot spots.

5. Elimination of local thermal bottlenecks by

(*a*). Use of larger conductors.

(*b*). Change of soil around conduit, as by replacing cinder fill.

(*c*). Relocating sources of external heat.

(*d*). Spreading of cables in crowded location.

6. Relocation of one or two cables to permit an increase in kilovolt-ampere loading of the remaining cables above the load carried on the removed cables.

7. Improvements in training of cable in manholes.

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1943 Supplement to Electrical Engineering—Transactions Section

Interim Report on Overloading Current-Limiting Reactors

AIEE TRANSFORMER SUBCOMMITTEE

Preface: The present war emergency requires that the maximum use be made of existing equipment and systems and that a minimum of critical material be used for new equipment.

This publication, as well as other guides and reports in this series, has been prepared for the information of users during the war emergency. Upon termination of the war emergency, they will be reconsidered by the Standards committee and the committees that prepared them, and will be approved, revised for normal use, or rescinded.

This procedure is being followed in preference to the preparation of special emergency Standards which might involve redesigning and drastic changes in manufacturing practices. These guides will accomplish the maximum conservation of critical materials, since they provide for the maximum use of existing equipment and systems, as well as new equipment, without changing the fundamental basis on which the present Standards have been prepared.

THIS report presents a general guide for loading current-limiting reactors above rating where specific design and operating data suitable to determine such loading are not available. It should be used in conjunction with the "Interim Report on Guides for Overloading Transformers and Voltage Regulators," presented on June 22, 1942, by the AIEE transformer subcommittee at the summer convention, which appeared in the September 1942 issue of ELECTRICAL ENGINEERING, as many data included there are not repeated in this report.

Temperature Limits

The present Standards for reactors provide that the temperature rise of the hottest spot of the windings shall not exceed 65 degrees centigrade for oil-insulated reactors and for dry-type reactors with Class *A* insulation or 90 degrees centigrade for dry-type reactors with Class *B* insulation, and that they shall be suitable for continuous operation at rated load, provided that the ambient temperature does not exceed 40 degrees centigrade and the daily average ambient temperature does not exceed 30 degrees centigrade.

This means that the total temperature of the hottest spot of the windings should not exceed a daily average of 95 degrees centigrade for oil-insulated reactors and dry-type reactors with Class *A* insulation or a daily average of 120 degrees centigrade for dry-type reactors with Class *B* insulation.

Reactors built to the present standards are often installed in compartments with restricted ventilation where the temperature of the reactor may be 10 to 15 degrees centigrade higher than when installed in the open. Under such conditions, dry-type reactors with Class *A* insulation are applied and operated under conditions that may result in an average daily hot-spot temperature of 105 degrees centigrade, and dry-type reactors with Class *B* insulation are applied and operated under conditions that may result in an average daily hot-spot temperature of 135 degrees centigrade.

Because of the aforementioned experience, it is undoubtedly safe to operate at these increased temperatures during this period. In order to prevent confusion in the case of reactors not enclosed in compartments, the ambient temperature is intended to mean the temperature of the air at a sufficient distance from the reactor not affected directly by the losses of the reactor. Likewise, in the case of reactors enclosed in compartments, it is not intended to refer to the temperature within the compartment but to the air surrounding the compartment.

Effect of Ambient Temperature on Continuous Loading for Normal Life Expectancy

The effect of ambient temperature on the loading of reactors is as follows:

If the daily average ambient temperature is below the reference of 30 degrees centigrade ambient, the loading may be increased, and, if the daily average ambient is above the reference ambient, the loading should be decreased as follows:

	Correction Per Cent Per Degree Centigrade Difference From the Reference
For oil-immersed reactors	1.0
For dry-type Class *A* reactors	0.75
For dry-type Class *B* reactors	0.5

Effect of Load Factor on Loading for Normal Life Expectancy

Load Factor

For daily load factors below 100 per cent, the loading may be increased 0.3 per cent for each per cent that the load factor is below 100 per cent, with normal life expectancy. Corrections in greater detail, based on an actual load curve, are not usually justified for normal loading. In no case should the overload permitted by this factor exceed 15 per cent, corresponding to 50 per cent daily load factor.

Paper 43-109, recommended by the AIEE committee on electrical machinery for presentation at the AIEE national technical meeting, Cleveland, Ohio, June 21–25, 1943. Manuscript submitted May 21, 1943; made available for printing June 6, 1943.

Personnel of the transformer subcommittee: M. S. Oldacre, *chairman;* F. S. Brown, E. S. Bundy represented by R. T. Henry, J. E. Clem, I. W. Gross, V. M. Montsinger, J. R. North, W. C. Sealey, F. J. Vogel, C. F. Wagner.

This interim report was prepared by the AIEE transformer subcommittee of the committee on electrical machinery for the purpose of making essential information immediately available to war industries, thus furthering the conservation of valuable material for the war emergency. It is educational and in no way mandatory. It is not intended as a "Standard," and has not been formally approved by the Standards committee nor the board of directors.

Emergency Short-Time Loading for Normal Life Expectancy

The following paragraph and Tables I and II indicate the short-time loads that can be carried without affecting normal life expectancy in 30 degrees centigrade daily average ambient.

Oil-Immersed Reactors

Oil-immersed reactors may be overloaded under emergency conditions in accordance with the curves given in American Standard *C*57.3, "Guides for Operation of Oil-Immersed Transformers."

Emergency Loading With Moderate Sacrifice of Life Expectancy

Tables III, IV, and V indicate the emergency overloads that can be carried with moderate sacrifice of life, probably not exceeding one per cent for each application, in 30 degrees centigrade daily average ambient.

Use of Other Corrections With Overload Values Given in Tables

Care should be taken that the percentage change from each of the various causes be based on the reactor current rating and not on the increased loading resulting from any of the other corrections.

The effects of the various following factors may be added directly:

For normal operation with normal life expectancy the effects of load factor, ambient temperature, and supplemental cooling may all be added in determining the load capability.

For short-time emergency operation with either normal life expectancy or moderate sacrifice of life, the effect of the actual ambient temperature during the emergency and of supplemental cooling may be added to the value for the emergency operation.

This method of adding corrections is not rigorously correct, but it is sufficiently correct for practical purposes and is simple to apply.

Table I. Dry-Type Class *A* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	120	1.21	1.40	1.65
2	115	1.10	1.17	1.27
4	110	1.04	1.06	1.08

* See footnote for Table V.

Table II. Dry-Type Class *B* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	150	1.15	1.35	1.60
2	145	1.07	1.15	1.25
4	140	1.03	1.05	1.07

* See footnote for Table V.

Supplemental Cooling

The design of dry-type reactors is such that forced-air cooling may be readily applied, provided sufficient space is available for the cooling equipment. For best results, the flow of air should pass over all the windings of the reactor. The permissible load can be increased in some cases as much as 30 per cent for the same temperature rise. The efficiency of forced-air cooling varies, and each application should be carefully investigated to determine the permissible increased loading.

Table III. Oil-Immersed Reactors

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current	
		Following Full Load	Following No Load
1	137	1.9	2.00
2	130	1.65	2.00
4	125	1.45	1.7
8	120	1.30	1.4
24	110	1.15	1.15

Table IV. Dry-Type Class *A* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	137	1.40	1.60	1.80
2	130	1.25	1.30	1.40
4	125	1.15	1.16	1.18
8	120	1.10	1.10	1.10

* See footnote for Table V.

Table V. Dry-Type Class *B* (Enclosed in Compartment)*

Duration of Load (Hours)	Hot-Spot Temperature (Degrees Centigrade)	Times Rated Load Current		
		Following Full Load	Following Three-Quarter Load	Following No Load
1	160	1.25	1.45	1.70
2	155	1.15	1.20	1.3
4	150	1.08	1.10	1.12
8	145	1.05	1.05	1.05

* Dry-type reactors with both Class *A* and Class *B* insulation installed in the open with three or more feet of head room will carry 7.5 per cent more load than when enclosed in compartments. In all cases where these conditions are not met the extra overload should be obtained from the manufacturer.

Cautions and Limitations

It must be recognized that overloads should not be applied to current-limiting reactors without a thorough study of the various limitations involved. If reactors are operating at higher than standard temperatures, the permissible duration of short-circuit may be reduced slightly. Among other limitations are: soldered connections, leads, effects of increased magnetic fields on surrounding equipment, as well as the thermal ability of connected equipment.

Before overloading current-limiting reactors to the full extent covered in this report, it is recommended that overload capabilities of the reactor be checked with the manufacturer.

Design Relationships for D-C Generators for Use in Aircraft

S. R. BERGMAN
ASSOCIATE AIEE

Synopsis: The paper sets forth the different operating conditions met with in aircraft generators as compared with standard types of industrial generators. It discusses certain features inherent in applications to aircraft, such as reliability and light weight. Certain requirements in both electrical and mechanical design are imposed by the fact that a plane is flying through varying altitudes resulting in changing atmospheric conditions, such as rarefication, low temperature, and ionization. Other unusual conditions are created from the motion of the plane setting up accelerating forces on the generator, as well as mechanical forces caused by engine vibrations. Because of important features such as commutation, voltage regulation, cooling, driving means, and light weight, special and unusual methods must be employed. In solving these problems the most perfect methods known in the art of building d-c machines must be resorted to, and the paper lays particular stress on the use of compensation.

Main and Accessory Engine-Driven Generators

THERE are two different methods of driving the generator:

1. The generator is driven from the main engine, in which case it is bolted to the engine and splined to a driving gear inside the engine. In some cases the generator is located in the nacelle and driven through a shafting containing two universal joints and a telescoping shaft so as to take up engine vibrations.

2. The generator is directly connected to an accessory engine. In this case the generator must be designed with relatively high efficiency so as to minimize the weight of gasoline consumed by the accessory engine.

The efficiency of main engine-driven generators runs between 70 and 80 per cent for different sizes (see Figure 3), and those driven by accessory engines should have an efficiency of from 85 to 90 per cent. The limitation of the application to accessory engines lies in the fact that they lose power quite rapidly with altitude since no satisfactory supercharger for this type of engine has as yet been developed.

Weights

Low weight is the natural requirement of all parts in airplanes. The weight of d-c generators for aircraft has been reduced to about one tenth of that of industrial generators of the same rating, and this has been accomplished with only a slight suffering in the efficiency. As an example: A d-c airplane generator rated 30 volts, 300 amperes, 4,400/10,000 rpm weighs 45 pounds—that is, five pounds per kilowatt—and has an efficiency of 77 per cent; an industrial-type generator of the same kilowatt rating at 1,750 rpm weighs about 400 pounds—that is, 45 pounds per kilowatt—and has an efficiency of 80 per cent. However, the airplane generator has at full load a temperature rise of 100 degrees centigrade employing an insulation consisting of glass and mica; whereas the industrial type of generator has a 40 degrees centigrade rise, using mainly cotton as a base insulation.

Future developments will be along the lines of better heat-resisting insulation. Varnishes have already been developed which will stand 200 degrees centigrade continuously without deterioration. Taking advantage of these higher temperatures, we may expect a further reduction in weight.

Influence of Atmospheric Conditions

The atmospheric conditions influence the cooling of the generator, affect the wear of the brushes, and because of rarefication and ionization influence insulation and creepages.

In the region in which the engine-driven generator is installed between the rear of the engine and fire wall, the ambient temperature is rather high, varying from 35 degrees centigrade to 65 degrees centigrade, and remains independent of the altitude. Since the density of the air decreases with the altitude, the cooling decreases in all self-ventilated generators. The permissible output is, therefore, reduced with the altitude and at a certain point, usually within the range of the plane, it becomes zero. Therefore, self-cooled engine-driven d-c generators are not advocated. Another type of cooling system consists of scooping the air from the outside of the plane and forcing this air through the generator. Since this cooling air has a very much lower temperature at high altitudes, it compensates for the decreased density. Investigation shows that the permissible output remains constant independent of the altitude. Blast cooling has, therefore, been generally adopted. In Figure 1 is shown how the ventilation is accomplished in a blast-cooled d-c generator.

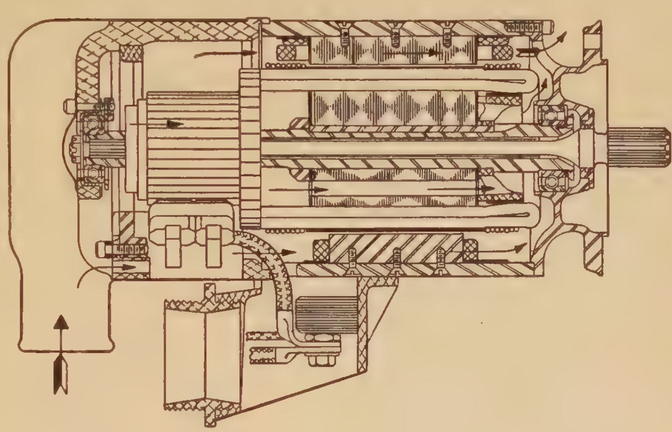

Figure 1. Assembly of d-c aircraft generator showing blast cooling

The influence of the atmospheric conditions at high altitude on the wear of the brushes is a serious one. While considerable progress has been made in the solution of this problem, it is not as yet fully solved. It is a well-known fact that a good commutator which has operated for some time at sea level builds up a glossy film, the surface of which consists mainly of carbon. At high altitudes, ordinary brushes have been found to wear down quite rapidly. Experiments undertaken in altitude chambers have shown this effect is related to the reduced moisture and oxygen content of the air, but special brush types have been found which give longer brush life. It has also been found that a cool commutator

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S. R. Bergman is consulting engineer, River Works, General Electric Company, West Lynn, Mass.

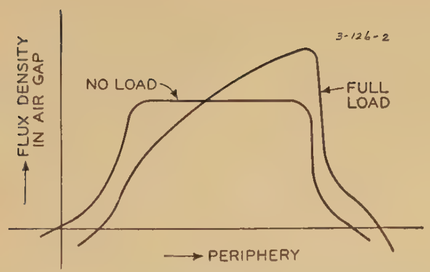

Figure 2. Flux distribution of a noncompensated d-c generator

and brushes are beneficial to brush life. Perfect commutation is important since it contributes to low temperatures. While it is difficult to give any definite rules for the design of commutators and brushes to give satisfactory brush wear, it may be stated that if the temperatures of commutator and brushes are limited to 100 degrees centigrade rise at sea level, and if the current density in the brush contact is also limited to less than 120 amperes per square inch, a reasonable brush life may be expected, provided that proper brushes are used.

The rarefication of the atmosphere at altitude in combination with ionization influences the breakdown voltage of the air. Between small spheres the breakdown voltage is proportional to the air density, for example, at 30,000 feet it is about one third of that at sea level. The ionization within the range of flight is comparatively small, and its influence on the sparkover voltage is probably quite small. However, if an arc is started, then, because of the great mobility of the charged particles, intense ionization will take place, and the arc will extend itself more quickly than at sea level. Therefore, it is quite important that no arcs or sparks are started. This requires three conditions to be fulfilled:

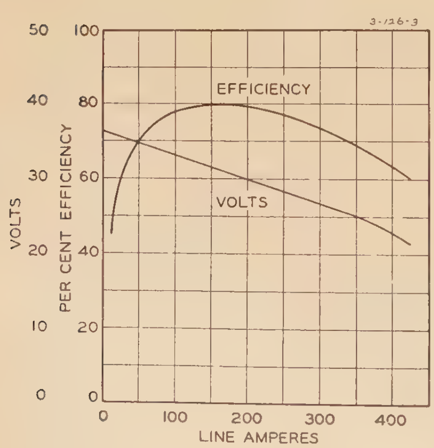

Figure 3. Regulation and efficiency curves of a compensated d-c generator

1. The commutation should be perfect.
2. The brush rig should be so designed as to prevent the sudden lifting of the brushes from vibrations.
3. Sufficient creepage distances should be provided for. For example, in a 30-volt generator the creepage distances should be no less than three sixteenths of an inch.

These circumstances must be taken into consideration when the voltage of the d-c system is determined. The 24-volt system has been found to be quite satisfactory. It has also been found that in certain motor generator sets which are used for turret and gun control 60 volts seems to be satisfactory. Engine-driven generators rated 120 volts have been built

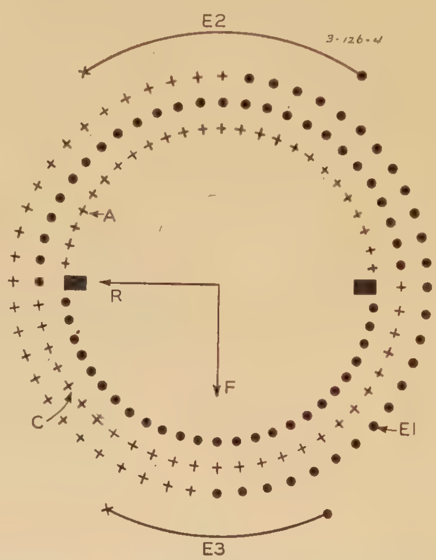

Figure 4. Schematic sketch of compensation and excitation

and mounted in the nacelle of flying boats; however, the ceiling for these ships is comparatively low, probably from 20,000 to 30,000 feet, and further experience will be required to evaluate fully the merits of voltages higher than the 30-volt system.

Vibrations

The vibrations of the engine are very severe, and the magnitude of the forces are not fully known. To guard against failure of the generator, any new design should have an engine test, preferably on the engine for which it has been designed. This run should not be less than 125 hours at different speeds. The engine vibrations are transmitted to the brushes of the generator, tending to throw them off the commutator. To prevent this, spring pressures should be comparatively high, but not so high as to cause excessive brush wear. It has been found that about

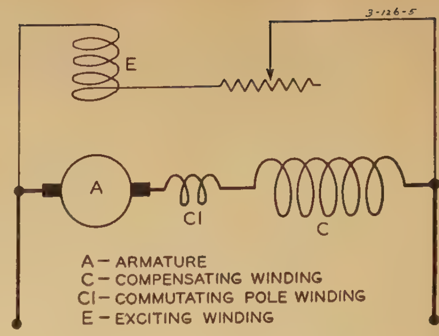

Figure 5. Winding diagram

five pounds per square inch of the brush contact is a good compromise. It is also advantageous to use a large number of small brushes, so that if a few brushes are thrown off, there are others to do the work. The inertia of the brushes should be made as low as possible to minimize these forces.

Another kind of vibration built up by the engine is the torsional vibration transmitted to the shaft. To take care of these vibrations a quill drive is employed (shown in Figure 1). In order to avoid resonance an asbestos packing is used between the inner and outer shafts, which serves as a damping medium. It may be pointed out that this is a nonwearing packing, since it does not slide but depends entirely upon internal molecular friction.

Electrical Characteristics

The electrical design must secure reliability of operation and light weight.

The reliability requires sparkless commutation at all speeds and for short heavy overloads; even short circuits should not be injurious to the commutator or brushes. In Figure 2 is shown a typical flux distribution curve in a noncompensated generator. It may be noted that

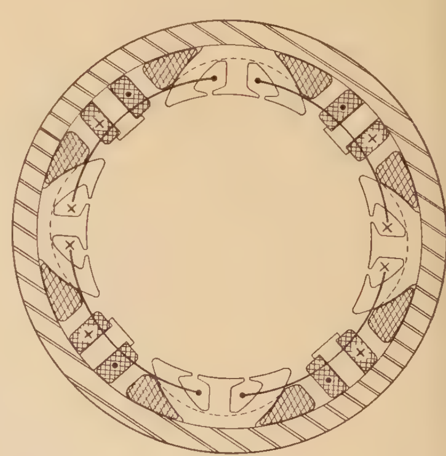

Figure 6. Structure of d-c generator with salient poles and compensation

at load the flux has been distorted in height and also shifted at the same time. This flux distortion is reflected in a similar distortion of the potential around the commutator, resulting in an increased maximum voltage between bars and also a piling up of the voltage toward one of the brushes. Under extreme conditions the maximum volts between bars may be increased from 30 to 40 per cent at full load, and near the brush it will be multiplied several times. Should excessive sparking occur, particularly when the air is rarefied and ionized, an arc may be formed and carried around to the next brush, very likely ruining the generator. This phenomenon is eliminated by the use of compensation, which has for its main object the stopping of flux distortion.

Since the heating is the limiting condition for light weight, an electrical design should be adopted which gives minimum losses. One solution which meets this requirement is the use of compensation, as it eliminates the load losses. Since compensation neutralizes the armature reaction, no field distortion takes place; hence, no extra core loss occurs. Compensation also eliminates sparking and thus does away with extra losses in the brush contacts and in the short-circuited coils. The extra losses in generators without compensation may be considerable, particularly in high-speed machines at weak field. In extreme cases it has been observed to amount to eight points in the efficiency.

Another feature of reliability of the generator is the proper shape of the regulation curve, showing the relationship between amperes load and the terminal voltage. As shown in Figure 3 this curve should be a uniformly drooping line, which is helpful in giving stable operation of the voltage regulator and also contributes to equal current distribution when several generators are operated in parallel, as is the case in multiengined planes. In Figure 3 is shown a typical regulation curve of a compensated generator, showing its uniformly drooping character.

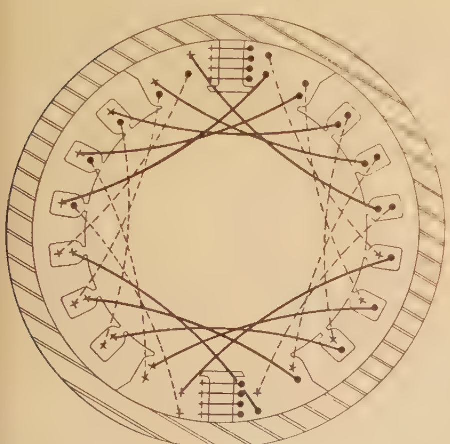

Figure 7. Structure of d-c generator with distributed windings

Figure 8. Six-pole field structure of compensated d-c generator

Compensation

The principle of compensation is shown schematically in Figures 4 and 5. Referring to Figures 4, *A* represents the armature winding which may be wound with any of the usual types of windings. Surrounding the armature is a field structure carrying the compensating winding *C*, which is connected in series with the armature, in opposition to it and equally as strong as the armature reaction *R*; thus these two windings constitute a nearly noninductive pair; that is, no flux except a small leakage flux will be produced. In order to commutate, a certain amount of commutating flux is necessary, and this flux is produced by overcompensation located on commutating poles, which for simplicity is not shown in Figure 4.

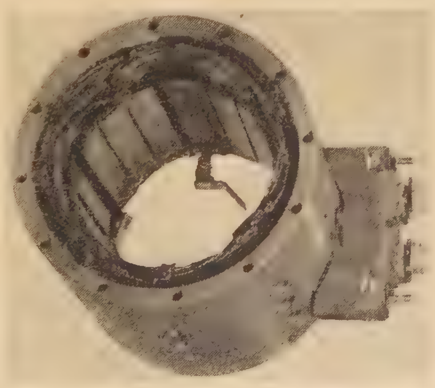

Figure 9. Complete field of four-pole compensated d-c generator

The field excitation *F* may be applied in two different ways:

1. In concentrated coils *E*2 and *E*3 assembled on salient poles.
2. It may be distributed in a winding *E*1 in which case we arrive at a d-c generator in which both the excitation and the compensation are fully distributed.

In Figure 6 is shown the structure of a d-c generator with salient poles and concentrated excitation. It also shows the compensating winding and the commutating poles with their windings.

In Figure 7 is shown the structure of a d-c generator with both the compensation and excitation distributed. Full drawn lines indicate the compensation, and dotted lines the excitation. It also shows the commutating poles with their windings. When inspecting the winding in Figure 7, it is of interest to observe that these windings are similar to a two-phase winding in a two-phase induction motor, one phase corresponding to the excitation and the other phase corresponding to the compensation. These two windings are 90 electrical degrees apart in space. It should also be noted that the pitch of these windings is 50 per cent, which secures short end windings and thus reduces losses.

Figure 10. Complete field of an accessory engine-driven d-c generator with fully distributed windings

In order to further illustrate the principle of compensation, reference is made to Figure 8, which is a photograph of a six-pole field structure rated 30 volts, 200 amperes, 2,500/5,000 rpm.

Figure 9 shows a photograph of a four-pole structure with four salient poles rated 30 volts, 300 amperes, 4,400/10,000 rpm

Figure 10 shows a photograph of a complete field with windings and brush holders of an eight-kw accessory engine-driven generator with fully distributed windings

Frequency Modulation for Power-Line Carrier Current

E. W. KENEFAKE
ASSOCIATE AIEE

FREQUENCY modulation is known to most people today as a system of broadcasting that is remarkably free from the effects of interference and is capable of providing high fidelity reception. These advantages have been realized largely by employing frequencies of 40 megacycles and higher which are well above the present broadcast band. A great amount of effort has been devoted to this field, and therefore, frequency modulation has been well analyzed both in theory[1–3] and experiment.[4,5] However, most of the work has been done with systems using a large deviation ratio which is usually defined as the ratio of the carrier deviation from normal to the highest modulating frequency.

This paper presents results of investigations carried on with a frequency-modulated system for power-line communication, employing frequencies below 200 kilocycles and a deviation ratio of one to one. In this way, the same band width as required for amplitude modulation, or *A-M*, can be utilized. The results of the investigation substantiate the work of others[9] and indicate that even with a one-to-one deviation ratio an *F-M* system is less disturbed by interference than a comparable *A-M* system. In addition, when such a system is subjected to the type of noise encountered on some power lines, the difference between *A-M* and *F-M* is still more prominent.

Outline of Theory

Unfortunately frequency modulation is not as widely understood as amplitude modulation. If *F-M* had been introduced and used for a number of years before *A-M*, the reverse would probably have been true. Both *F-M* and *A-M* are descriptive of the manner in which a modulating quantity alters a sine function that represents a carrier frequency. Since a carrier frequency is an alternating quantity having both amplitude and frequency as its principal characteristics, it is possible to operate on either one of these to cause the carrier frequency to carry intelligence.

One effective way to compare two different modulation schemes is by the equations for the modulated signal. The equation for an *A-M* signal is merely a sine function whose amplitude, A, is varied in some fashion and is usually expressed as

$$e=A(1+m\sin\alpha t)\sin\omega t \tag{1}$$

where

$\omega=2\pi$ times the carrier frequency
$\alpha=2\pi$ times the modulating frequency
$m=$amplitude of the modulating frequency and is one for 100 per cent modulation

This equation can be expanded by trigonometric identities into three separate sine functions

$$e=A\sin\omega t+\frac{mA}{2}\cos(\omega-\alpha)t-\frac{mA}{2}\times\cos(\omega+\alpha)t \tag{2}$$

which can be used to determine the band width of various type channels.

In a similar manner, the equations for a frequency-modulated signal can be developed. Since this has often been done in the literature,[1,3] the important steps only will be given. The equation for the modulated signal is

$$e=A\sin\left(\omega t+\frac{\Delta\omega}{\alpha}\sin\alpha t\right) \tag{3}$$

To grasp the significance of this equation, it is only necessary to remember that $\Delta\omega$ is directly proportional to the amplitude of the modulating frequency. As the amplitude of a sine function is varied in equation 1 for an *A-M* signal, the frequency of a sine function is varied in equation 3 for a frequency-modulated signal. This last equation can be expanded by means of identities into

$$\begin{aligned}e=A\Bigg\{&J_0\left(\frac{\Delta\omega}{\alpha}\right)\sin\omega t+\\&J_1\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+\alpha)t-\sin(\omega-\alpha)t]+\\&J_2\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+2\alpha)t+\sin(\omega-2\alpha)t]+\\&\ldots\ldots\ldots\ldots\ldots\ldots+\\&J_n\left(\frac{\Delta\omega}{\alpha}\right)[\sin(\omega+n\alpha)t+(-1)^n\sin(\omega-n\alpha)t]\Bigg\}\end{aligned} \tag{4}$$

where $J_n\left(\frac{\Delta\omega}{\alpha}\right)$ is the nth order Bessel function of the first kind.

Therefore, instead of one upper and one lower side band as in equation 2, the possibility of many side bands exist. The magnitude of these side bands are fixed by the Bessel functions $J_0\left(\frac{\Delta\omega}{\alpha}\right)$ to $J_n\left(\frac{\Delta\omega}{\alpha}\right)$. However, if $\frac{\Delta\omega}{\alpha}\leq 1$, only the first two terms are significant, and the values of the Bessel functions of the second to nth order

$$\left[J_2\left(\frac{\Delta\omega}{\alpha}\right)\text{to } J_n\left(\frac{\Delta\omega}{\alpha}\right)\right]$$

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E. W. KENEFAKE is in the radio transmitter engineering division, electronics department, General Electric Company, Schenectady, N. Y.

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rated 30 volts, eight kw, 4,000 rpm.

Both the salient pole construction and the field using distributed windings are being used for aircraft generators. It has been found that the salient pole structure is the lightest of the two structures by about 10 per cent. Since weight is of paramount importance, the salient pole construction is now given preference. While both methods lead to very good commutation, the distributed windings seem to be the most powerful means of obtaining perfect operation, the reason being that it minimizes the leakage and secures a very clean neutral. As an example of how powerful the combination of a distributed compensation and excitation winding is, the author built a separately excited d-c generator rated 150 kw, 15,000 volts, having an average volts per bar of 90, which at full voltage withstood dead short circuits with only a minor flicker on the commutator.

Large industrial machines which have to meet severe operating conditions have been made for many years with compensation. In this paper it has been proved that small d-c generators of the compensated type can be made in a very compact form with light weight.

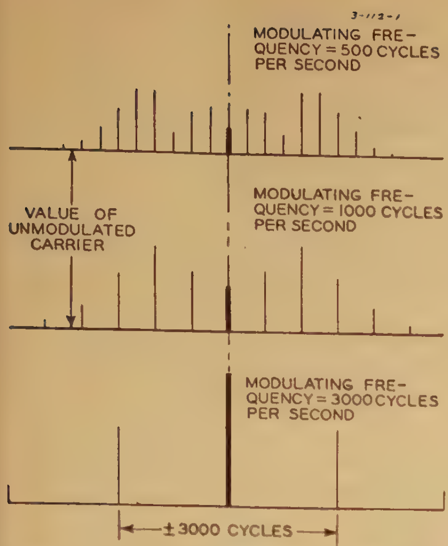

Figure 1. Chart of the amplitudes of the side bands for three different modulating frequencies when the deviation ratio is one to one

are small. Advantage is taken of this fact in the system discussed in this paper.

Another type of modulation has been used that is closely related to *F-M*, namely, phase modulation or *P-M*.[6] Actually, a frequency shift always changes the rate of phase change and vice versa so that the two functions are dependent on each other. By definition, the two types of modulation, *F-M* and *P-M*, have become associated with the nature of the modulated signal. The equation for a phase-modulated signal, comparable to equation 3, is

$$e = A \sin (\omega t + \phi \sin \alpha t) \qquad (5)$$

This equation can be expanded to be the same as equation 4 except with ϕ substituted for $\frac{\Delta\omega}{\alpha}$. Both of these terms have the dimension of phase angle. For the same intensity of all modulating frequencies, in phase modulation the phase shift is constant, but the frequency shift is proportional to the modulating frequency; while in frequency modulation, the frequency shift is constant for all modulating frequencies and the phase shift is inversely proportional to the modulating frequency. Also, the band width of the former is approximately proportional to the modulating frequency, and the band width of the latter is practically constant. Sufficient swing or modulation cannot be obtained easily with phase modulation, and, since reactance tubes can produce enough frequency modulation at low carrier frequencies, strictly frequency modulation was used in the investigation reported here.

Deviation Ratio

The choice of a one-to-one deviation ratio was influenced principally by the desire to restrict the band width of individual channels to the same space that would be required for a comparable *A-M* channel. This is quite important from the standpoint of any user of power-line carrier. The art has progressed to such a stage that many pieces of auxiliary apparatus have been designed and standardized around amplitude-modulated systems. Therefore, existing apparatus as well as equipment already in production could be used with an *F-M* transmitter and receiver to set up a channel. In other words, if the band-width characteristics of line traps and tuning units are applicable to *F-M*, then there would be no change in the application and installation practices already in use.

Even with a one-to-one deviation ratio, the frequency shift is a large percentage of the carrier as compared to the per cent frequency shift used in present day *F-M* broadcast practice. For instance, a 3,000-cycle shift is six per cent of a carrier frequency of 50 kilocycles, whereas the standard 75-kilocycle shift of a broadcast transmitter at 40 megacycles is only about 0.2 per cent. A greater deviation could be generated at these low frequencies, but the band-width requirements of the receiver and coupling apparatus would be excessive. On the other hand, a smaller deviation would not reduce the required band width proportionately and would only result in a lower degree of modulation.

A better picture of what a one-to-one deviation ratio produces in the way of side bands is given in Figure 1, which shows a chart of the amplitudes of the various side bands for different modulating frequencies. This chart was made up by plotting the amplitudes of the various terms given in equation 4, in which $\Delta\omega$ corresponds to a frequency of 3,000 cycles in all cases. From this diagram it can be seen that the bulk of the side bands are confined to a band width of ±3,000 cycles. Of course, if the utmost in fidelity is desired, some of the side bands extending beyond ±3,000 cycles would have to be transmitted and received. Actually, if all the side bands outside of the ±3,000 cycle band were eliminated, only about four per cent of the total energy in the modulated wave would be lost. This would cause a small amplitude modulation of the carrier to appear, but the limiter in the receiver restores the carrier to a constant amplitude. However, the distortion resulting from eliminating the higher side bands is small compared to the total distortion in any *A-M* carrier channel.

Noise Considerations

Noise reduction by means of frequency modulation has been the largest factor in promoting broadcast *F-M*. Interference to the reception of a desired signal may come from many sources and be of various types. In space radio, the most uncontrollable interference is that which takes the form of modulated frequencies in the band accepted by the receiver. The energy in these frequencies is usually uniformly distributed over the band of any channel and is often termed external random noise. With varying magnitudes of this type noise, *F-M* has the characteristic of keeping the interference well rejected until the magnitude of the noise bears a certain ratio with respect to the desired signal. Then the noise is accepted, and the desired signal is rejected. In wide-swing or large-deviation *F-M* this threshold is quite abrupt and occurs when the signal-to-noise ratio becomes about two. It has been demonstrated[3] that in

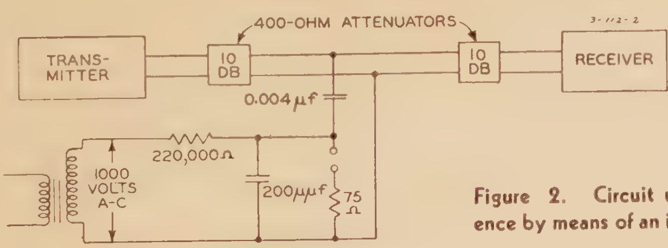

Figure 2. Circuit used to produce interference by means of an intermittent arcing air gap

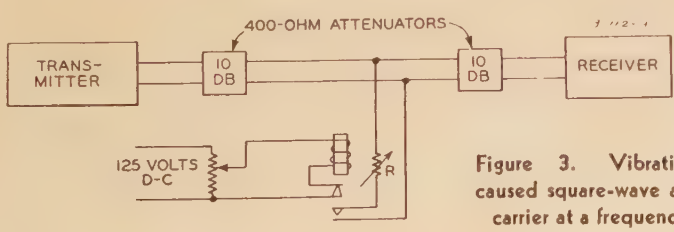

Figure 3. Vibrating-relay circuit which caused square-wave amplitude modulation of carrier at a frequency of about 100 cycles

a small-deviation system, this threshold is not as abrupt and occurs at a lower signal-to-noise ratio. Therefore, it develops that a small-deviation system can deliver usable intelligence from a signal whose signal-to-noise ratio is less than two. This is a definite advantage when the primary function of a service is to get intelligence from one point to another.

There always exists on power lines a fairly high level of random noise caused by corona and arcs of various types acting as sources of amplitude-modulated radio frequencies. In these cases more transmitter power helps in improving the signal-to-noise ratio. Noise of this nature when added to the desired carrier causes a complex type of amplitude variation and phase shift. This produces a signal which is similar to a carrier and a single side band. It is possible for the limiter in an *F-M* receiver to remove the amplitude modulation, but of course it is impossible to eliminate the phase shift. Therefore, it is this phase shift that is the source of noise for an *F-M* channel. It is possible in an *A-M* system for noise to cause a 100 per cent modulation of the carrier, and this is also the limit to the degree of modulation by the desired signal. In frequency modulation it is impossible for noise to cause a phase shift of more than about one radian, but it is possible for the desired signal to cause a much greater phase shift. The phase shift is one radian for the highest value of α when a one-to-one deviation ratio is used. This phase shift becomes greater as α decreases and is the reason for more noise interference present at the higher modulating frequencies. By assuming a random-type noise, the noise reduction theoretically possible by means of *F-M* over *A-M* is six decibels. This is computed from the fact that noise in *A-M* is the same over the audio spectrum while in *F-M* the noise is zero at zero audio frequency and increases linearly to the same value as in *A-M* at the highest audio frequency. Hence, when the noise is integrated over the audio range, it is just half as great in *F-M* as in *A-M*.

Random noise may not be the only type of interference encountered in carrier current or any system where metallic circuits of one kind or another are used for the medium of transmission. In fact, another type of interference, apparently caused by corona, has also been observed on power lines. The carrier in traversing the power line becomes amplitude modulated with noise, and for lack of a better name this phenomenon has been called "corona modulation." The modulation is apparently a result of corona producing an impedance change to the flow of the carrier-current signal. If only this type of noise is present, an increase in transmitter power to surmount the noise does not help because the per cent modulation from the noise stays the same. This modulation is a function of the total attenuation between the transmitter and the receiver and the amount of corona present on the lines. A simple experiment that can be performed to detect this type of noise is to listen to a signal in a carrier receiver with no automatic volume control in operation. When the transmitter is turned off, this type of noise will decrease or completely disappear, indicating that it comes in as modulation of the carrier signal. In this case, the noise is carried by symmetrical side bands about the carrier as represented by the last two terms of equation 2. However, when this type of noise is fed into an *F-M* receiver which contains a balanced discriminator, these side bands are balanced out, since a discriminator is a slope filter whose output is proportional to the frequency deviation from normal. Because an *A-M* signal has amplitude variations but no phase or frequency variations, the output of the discriminator in this case would be zero. Herein is the outstanding advantage of frequency modulation for power-line carrier because it provides the only real solution to corona-modulation noise.

In an effort to improve the discrimination against interference in an *F-M* system still further, advantage may be taken of the energy versus frequency characteristic of the voice.[11] This characteristic indicates that the bulk of the energy in voice exists in the low audio frequencies. Therefore, it is possible to pre-emphasize the higher audio frequencies in the transmitter without exceeding the modulation capabilities of the transmitter. This, of course, makes it necessary to use a de-emphasis circuit in the receiver. Then the scheme is in effect a combination of phase modulation and frequency modulation. It provides a means to get the greatest frequency shift for any modulating frequency without exceeding the band width of any particular channel.

Noise Measurements

With these known facts, a preliminary setup was made in the laboratory consisting of one transmitter and one receiver. The functioning of various circuits was checked, as well as the characteristics of *F-M* when noise was present. These initial results were so promising that a complete communication channel was later set up in the laboratory which involved two transmitters and two receivers and some auxiliary equipment with which more elaborate measurements were made. After considerable development work, a third group of samples was built which embodied all of the improvements and circuit designs worked out in the previous samples. The final samples involved commonly used *F-M* circuits such as a pre-emphasized frequency modulator, a modulation limiter, and a class *C* amplifier in the transmitter; with a radio-frequency amplifier, a limiter, a discriminator, a de-emphasized audio amplifier and a carrier-off noise suppressor in the receiver. Naturally, certain modulations and improvements were made in order to adapt these *F-M* circuits to low-frequency operation.

Extensive tests were made with the samples, of which the noise-reduction measurements were the most interesting. Various schemes were used to produce noise, but the two that proved most acceptable are shown in Figures 2 and 3. The circuit shown in Figure 2 introduced a type of noise which was principally characterized by an arc as a source of radio-frequency energy. The circuit in Figure 3 amplitude-modulated the carrier with a square wave at a rate of about 100 cycles so that several simultaneous noise side bands were produced. Measurements were made to indicate differences between an *F-M* channel and *A-M* channel having the same transmitter power, band width, audio characteristic and attenuation between transmitter and receiver. These measurements were made after both systems were set up in such a way that a 500-cycle modulating frequency produced the same output from both receivers and caused 100 per cent modulation of the *A-M* transmitter and a deviation of 3,000 cycles in the *F-M* transmitter. This particular audio frequency was used because it was just below the point in frequency where the pre-emphasis and de-emphasis circuits began to function. Table I gives the amount the noise level in *F-M* was found below the noise level in *A-M* in decibels. The results in Table I are an average of several measurements with each of the samples mentioned.

Table I

Type of Noise	Unemphasized F-M	Pre-emphasized F-M
Figure 2	11	16 to 19
Figure 3	19	23 to 25

The results show considerably more noise reduction than theory predicted. The theory is based on the assumption that the noise is random, while circuits used here did not necessarily produce random noise. It is felt that the noise encountered on power lines is similar to some combination of the two types of noise employed in these tests. It may be all of one type or any combination of the two. The noise reduction is greater than expected, principally since the bulk of the energy in the noise is in the lower audio frequencies.

Based on these noise measurements made with the arc-noise generator in Figure 2, which utilizes the less favorable type of noise, it would take the following transmitter powers to give the same signal-to-noise ratio in a receiver under the same conditions.

System	Watts
AM	100
Unemphasized *FM*	8
Pre-emphasized *FM*	2

This indicates the possibility of greater transmission ranges with medium power levels which may allow some communication problems to be solved without repeating stations.

Other Advantages

Noise reduction is not the only achievement of frequency modulation as far as communication for power-line carrier is concerned. There are other characteristics which can be utilized to advantage in the various types of communication. The presence of a limiter in the *F-M* receiver is analogous to a very fast and flat type of automatic volume control. The automatic volume control used in *A-M* receivers is of necessity reasonably slow and usually not very flat. One type of communication wherein this characteristic is advantageous is the two-frequency duplex where one frequency is used to transmit in one direction and the other frequency is used to transmit in the opposite direction. The transmitter and receiver audio circuits must then connect to a hybrid circuit and can be set closer to the critical point if the audio output of the receiver is quite constant. Since the limiter in an *F-M* receiver can hold the output extremely constant, a two-frequency voice channel can be operated with higher audio gains, from terminal to terminal.

The other type of communication commonly used is the single frequency duplex or automatic simplex where the voice starts the transmitter and blocks the receiver on outgoing speech. To reduce the amount of clipping of words, it is desirable to accomplish the switching of the transmitter and receiver in as short a time as possible. One inherent limitation in an *A-M* system for fast switching of a channel is the surge of rectified carrier in the detector of a receiver when the transmitter starts. For smooth operation it is necessary to hold the receiver in an inoperative condition until this surge dies down. This difficulty cannot be completely overcome with an *A-M* system because turning the transmitter on and off is fundamentally amplitude modulation, and the detector is designed to detect amplitude modulation. When *F-M* is used, the detector in the receiver is insensitive to amplitude variations and is only sensitive to any phase variations that may come about in starting and stopping the transmitter. Since any phase shifts are small compared to the phase shift caused by modulation by the desired signal, this surge is greatly minimized in the receiver when *F-M* is used.

The transients in an automatic-volume-control circuit of an *A-M* receiver at the moment carrier is received prevents the possibility of opening the receiver quickly and getting an undistorted signal in the output. These transients in automatic-volume-control circuits change the gain of the receiver and hence the output of the receiver is modulated by these transients which causes distortion. On the other hand, a limiter in an *F-M* receiver is practically instantaneous in action, and the receiver can be opened very rapidly without the possibility of the receiver output becoming distorted by transients.

It has been shown[12,13] that two broadcast *F-M* stations operating on the same frequency have considerably more interference-free operating area than two *A-M* broadcast stations on the same frequency. This would still be true of two *F-M* power-line carrier communicaton channels, except to a lesser degree, since the deviation ratio is one to one instead of five to one. With the rapid installation of carrier-current apparatus throughout the country, there are many places where frequency congestion is becoming a problem. One partial solution to this problem would be by means of *F-M*, since a certain frequency could be repeated throughout a system more often than with amplitude modulation.

This paper has largely dealt with aspects of frequency modulation as applied to voice communication. Obviously, there are certain other services where *F-M* is much better than *A-M*. It has been demonstrated that a frequency-shift-type signaling channel[10] is superior in most all respects to a keyed-carrier type of signal channel. It is much easier to transmit frequency or phase accurately through nonlinear tube circuits than it is to transmit an amplitude faithfully through these same type circuits. Therefore, frequency modulation seems basically advantageous when tube circuits are involved. Also, all radio-frequency circuits associated with an amplitude-modulated signal must be linear whereas they need not be linear with a frequency-modulated signal.

Conclusion

It seems certain that the characteristics of *F-M* make it desirable for many applications in the carrier-current field. The outstanding characteristic of *F-M* is the rejection of the interference resulting from undesired amplitude modulation of the carrier.

The chief advantages for *F-M* when applied to power-lines may be summarized as follows:

1. Capability of noise reduction.

2. Possibility of longer channels or less power in transmitters.

3. Less interference between channels on the same frequency.

4. Flat automatic volume control in receiver.

5. Fast switching of channel possible in single frequency voice communication.

6. Same auxiliary apparatus as used for *A-M* channels because of the same band-width requirements.

These advantages have been discussed to a considerable extent in the body of the paper.

It should be pointed out that *F-M* equipment cannot be applied to operate with existing *A-M* equipment. A signal from an *F-M* transmitter can be reproduced by an *A-M* receiver by tuning the receiver to one side of the carrier[8] so that the selectivity characteristic performs similar to a slope filter. However, this is not acceptable for general application because of the distortion resulting from the nonlinearity and narrowness of the sloping part of the characteristic. Also, any frequency drift which may occur in the *F-M* carrier results in excessive distortion in the *A-M* receiver because the slope filter which is obtained by the one-

The Calculation of Unbalanced Magnetic Pull in Synchronous and Induction Motors

R. C. ROBINSON
ASSOCIATE AIEE

Synopsis: Unbalanced magnetic pull may be defined as the net sideways force between the stator and rotor of an electric machine resulting from a difference in the air-gap flux densities on opposite sides of the machine. This difference in flux density is, in general, caused by a difference in the air gaps on the two sides.

There is outlined herein a method of calculation of unbalanced pull which takes into account the combined effects of saturation, parallels, and primary reactance. The increased accuracy which this method affords should make possible more accurate predictions of mechanical deflections and critical speeds.

I. Induction Motors

INDUCTION motors may operate at any speed from zero to synchronous speed and at any applied voltage from zero to 110 per cent of normal. Operation of the machine under these conditions may be illustrated on the no-load saturation curve. The no-load saturation curve will be represented by a power equation of the form:

$$i_m = eI_g + e^m I_s$$

Assume that the flux density over a certain section of the air gap of a machine is uniform and that a uniform change is made in the length of the air gap over this section while the magnetizing current is maintained at a fixed value. It is shown in Appendix I that the resulting change in gap density is given by the equation:

$$b = \frac{\left(1 - \dfrac{K_c'}{K_c}\dfrac{g-\Delta}{g}\right)eB_g}{\dfrac{K_c'}{K_c}\dfrac{g-\Delta}{g} + me^{m-1}\dfrac{I_s}{I_g}}$$

This equation may be used to give the rise in gap density caused by a given change in the air gap for an induction motor with a series or two-parallel primary winding operating at any speed and voltage. However, it should be noted that the no-load saturation curve does not accurately represent the flux conditions in the motor under load. Use of this curve is justified, however, by the fact that the total saturation will normally be divided fairly evenly between the stator and the rotor, and the density rise as calculated by the foregoing equation will be nearly correct.

If a very small deflection is considered, this equation has the following form:

$$b = \frac{\dfrac{\Delta}{g}eB_g}{1 + me^{m-1}\dfrac{I_s}{I_g}}$$

This equation may be written in the form:

$$b = K_s\frac{\Delta}{g}eB_g$$

where

$$K_s = \frac{1}{1 + me^{m-1}\dfrac{I_s}{I_g}}$$

and this represents the effect of saturation on the flux density rise.

A. Unbalanced Pull for a Series or Two-Parallel Primary Winding

Consider now a machine having constant flux density around the periphery in which the rotor is displaced to one side a small amount.

The deflection will then be approximately sinusoidal around the periphery, and the flux density change will also be approximately sinusoidal, since $b = \text{constant} \times \Delta$.

The fundamental formula for magnetic pull is:

$$\text{Magnetic pull} = 0.01386B^2A$$

Using this formula together with the equation for density rise, it is easily proved that:

$$\text{Unbalanced magnetic pull} = 0.01386K_s\frac{\Delta}{g}e^2B_g^2A$$

In another form, this may be written:

$$\text{Unbalanced magnetic pull} = \text{constant} \times \frac{e^2}{1 + me^{m-1}I_s}$$

Solving for a maximum:

$$E^{m-1} = \frac{2}{(m^2 - 3m)\dfrac{I_s}{I_g}}$$

where E is the per unit voltage at which maximum pull occurs.

This formula gives the voltage at which maximum pull would occur if the gap density were uniform around the periphery of the machine. However, with a sinusoidal wave form and with part of the saturation in the teeth it is apparent that maximum pull for the entire wave will not occur when the peak density reaches the aforementioned value for maximum pull but will occur at some higher density. For an unsaturated machine, when the peak density reaches the aforementioned value, the average pull

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R. C. Robinson is electrical design engineer in the a-c generator engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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sided tuning of an *A-M* receiver is very narrow.

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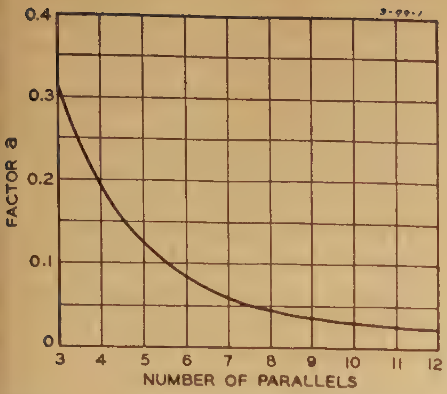


Figure 1. Factor *a* plotted against the number of parallels

will be one half the pull that would be obtained if the density were uniform at this value. However, with saturation, it is a close approximation to take this value as two thirds of the pull calculated with a uniform density. There is an important exception, however; that is, that, when the maximum pull comes at higher than 110 per cent voltage, the unsaturated value of one half should be used.

This gives two cases as follows:

1. For E less than 1.1

Maximum unbalanced pull =

$$2.88K_sE^2\frac{B_g{}^2A}{g}\times 10^{-4}$$

2. For E greater than 1.1

Maximum unbalanced pull =

$$2.62K_s\frac{B_g{}^2A}{g}\times 10^{-4}$$

Both formulas apply to a deflection of $^1/_{32}$ inch.

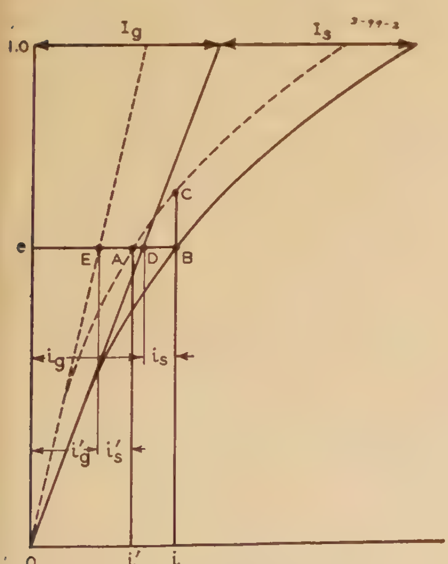


Figure 2. Air-gap flux-density change from the no-load saturation curve

B. Unbalanced Pull for Three or More Parallels in the Primary Winding

Assume a constant gap density under one parallel of the winding, and assume that the gap is changed a constant amount over this section. It is shown in Appendix II that the flux density rise is given by the formula:

$$b=\frac{\frac{\Delta}{g}eB_g}{1+me^{m-1}\frac{I_s}{I_g}+\frac{1}{I_gX_1}}$$

This may also be written in the form:

$$b=K_R\frac{\Delta}{g}eB_g$$

where

$$K_R=\frac{1}{1+me^{m-1}\frac{I_s}{I_g}+\frac{1}{I_gX_1}}$$

and represents the reduction in density rise because of saturation and primary reactance.

It is shown in Appendix III that if there are n parallels and r be any one of these parallels, the total pull of the rth parallel along a given line is given by the formula:

Total pull of rth parallel =

$$1.84\times 10^{-4}e^2\frac{B_g{}^2A}{g}\left[\frac{\sin r\frac{4\pi}{n}-\sin (r-1)\frac{4\pi}{n}}{4}\times K_s+(K_R-K_S)\frac{n}{2\pi}\left\{\sin r\frac{2\pi}{n}-\sin (r-1)\frac{2\pi}{n}\right\}^2+\frac{\pi}{n}K_s\right]$$

For various numbers of parallels, this formula has the following forms:

3 parallels: unbalanced pull =

$$2.88\times 10^{-4}e^2\frac{B_g{}^2A}{g}[0.315K_s+0.685K_R]$$

4 parallels: unbalanced pull =

$$2.88\times 10^{-4}e^2\frac{B_g{}^2A}{g}[0.188K_s+0.812K_R]$$

6 parallels: unbalanced pull =

$$2.88\times 10^{-4}e^2\frac{B_g{}^2A}{g}[0.087K_s+0.913K_R]$$

8 parallels: unbalanced pull =

$$2.88\times 10^{-4}e^2\frac{B_g{}^2A}{g}[0.045K_s+0.955K_R]$$

12 parallels: unbalanced pull =

$$2.88\times 10^{-4}e^2\frac{B_g{}^2A}{g}[0.022K_s+0.978K_R]$$

Infinite parallels: unbalanced pull =

$$2.88\times 10^{-4}e^2\frac{B_g{}^2A}{g}[0K_s+1.0K_R]$$

Generally, the unbalanced pull may be expressed in the following form:

Unbalanced pull =

$$2.88e^2\frac{B_g{}^2A}{g}[aK_s+(1-a)K_R]\times 10^{-4}$$

where a is determined from the curve of Figure 1.

From this equation, it is practically impossible to solve analytically for the voltage at which maximum pull will occur. However, a close approximation will be obtained, if we use in this formula the voltage at which the pull is a maximum, considering saturation alone.

With the constants that are used in most induction motors, the factor $[aK_s+(1-a)K_R]$ is approximately equal to K_s divided by the number of parallels. Therefore, the important fact is established that the unbalanced pull for an induction motor having three or more parallels in the primary winding may be obtained approximately by calculating the pull for a series winding and then dividing by the number of parallels.

II. Synchronous Motors

Synchronous motors have three distinct modes of operation as follows:

1. On open circuit with any speed from zero to synchronous and any field current from zero to full load field current.

2. Starting as an induction motor with any speed from zero to synchronous and any applied voltage from zero to 110 per cent of normal.

3. Normal synchronous operation with any applied voltage from zero to 110 per cent of normal, any field current from zero to full-load field current and any load.

A. Unbalanced Pull for a Series or Two-Parallel Stator Winding

A synchronous motor with a series winding on open circuit follows the same type of analysis as the induction motor

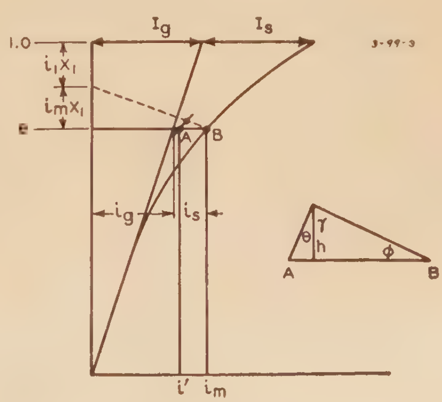


Figure 3. Flux-density change in one parallel of an induction motor

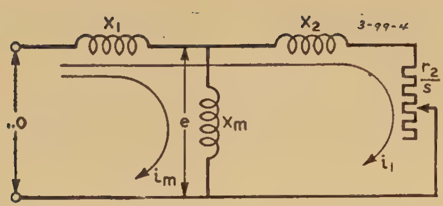


Figure 4. Equivalent circuit of the induction motor

with a series winding. Figure 2 and the equations derived from it apply except that they must be modified to take into account the field form of the synchronous motor. It has been found that this may be done quite accurately by multiplying the formulas for unbalanced pull previously given by C_1, the form factor of the wave.

Under conditions of starting or normal running, the relation between the air-gap flux density and the density in the iron parts of the machine may be different than the relation shown on the no-load saturation curve of Figure 2. However, as with induction motors, part of the iron carries higher flux than the air gap, while part carries lower. This tends to cancel out, and the magnetic pull calculated from the no-load saturation curve will be nearly correct for most machines.

Synchronous motors will normally be limited to about 120 per cent of normal air-gap flux density.

The following formulas, therefore, apply to a synchronous motor with a series or two-parallel stator winding:

1. For E less than 1.2

Maximum unbalanced pull =

$$2.88K_sE^2\frac{B_g{}^2A}{g}\,C_1\times10^{-4}$$

2. For E greater than 1.2

Maximum unbalanced pull =

$$3.11K_s\frac{B_g{}^2A}{g}\,C_1\times10^{-4}$$

B. Unbalanced Pull for Three or More Parallels in the Stator Winding

There is one very important difference between unbalanced pull in induction and in synchronous motors when parallels are present. That is that the parallels will not reduce the pull in a synchronous motor at standstill on open circuit and with the field excited. For a consideration of pulling-over of the rotor, this is then the worst condition of operation, and the machine must be calculated as though it had only a series winding in the stator. However, for a consideration of critical speed of the shaft the running condition must be analyzed, and it is with this problem that the remainder of this section will deal.

A synchronous motor during the starting period will have an unbalanced magnetic pull which can be calculated in the same way as an induction motor. The following formulas will apply:

1. For E less than 1.1

Maximum unbalanced pull =

$$2.88E^2\frac{B_g{}^2A}{g}\,[aK_s+(1-a)K_R]\times10^{-4}$$

2. For E greater than 1.1

Maximum unbalanced pull =

$$2.62\frac{B_g{}^2A}{g}\,[aK_s+(1-a)K_R]\times10^{-4}$$

In the case of a synchronous motor it will be necessary to use these two formulas rather than merely dividing the unbalanced pull for a series winding by the number of parallels. This is due to the fact that the magnetizing current is normally much larger in synchronous than in induction motors.

Consider now the case of a synchronous motor running on open circuit with any field current and with theoretically an infinite number of parallels. It is apparent that with a change in air gap, the flux density rise will be $X_1/(X_1+X_m)$ times the density rise that would have occurred if no parallels had been present. The quantity $X_1, K_s/(X_1+X_m)$ therefore becomes the factor K_R for this condition of operation. The resistance of the stator winding has been neglected, but it should be noted that as zero speed is approached, the resistance limits the circulating current and brings the pull up to the full locked rotor value.

Suppose that the motor is now operated synchronously with a fixed voltage, field current, and load, and a small change is made in the air gap by displacing the rotor to one side. The reduction in air gap on one side releases a certain number of field ampere turns and a certain number of armature reaction ampere turns to force more flux through the machine. This tends to raise the flux in the air gap, but this increase in gap flux produces a current component in the stator parallel (not necessarily in phase with the main current) which produces a leakage reactance plus armature reaction voltage drop exactly equivalent to the original density rise. The phase angle of the current increment is such as to produce an armature reaction exactly in opposition to the original density rise. The net density rise will then be $X_1/(X_1+X_m)$ times the original density rise which gives the same K_R as for the open-circuit condition. As before, use of the no-load saturation curve for the determination of K_s is not exact but is a justifiable approximation.

Since the gap density will be limited to about 120 per cent of normal, we have two cases as follows:

1. For E less than 1.2

Maximum unbalanced pull =

$$2.88E^2\frac{B_g{}^2A}{g}\,[aK_s+(1-a)K_R]C_1\times10^{-4}$$

2. For E greater than 1.2

Maximum unbalanced pull =

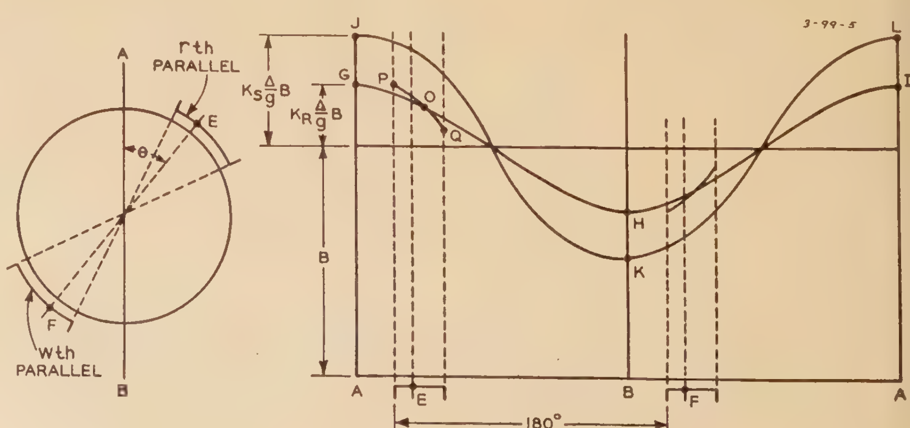
$$3.11\frac{B_g{}^2A}{g}\,[aK_s+(1-a)K_R]C_1\times10^{-4}$$

where

$$K_R=\frac{X_1}{X_1+X_m}\,K_s$$

For most machines, it is found that there is a larger magnetic pull during synchronous operation than during the starting period. Consequently, only this condition need be calculated in order to obtain the worst possible unbalanced pull.

Figure 5. Effect of parallels on the flux density change



Summary

Formulas have been given for calculating first, the per unit voltage at which maximum pull occurs, and then the maximum unbalanced magnetic pull for a synchronous or induction motor with any number of parallels in the stator winding. All quantities used in the formulas may be taken directly from the design sheet with the exception of the exponent m which may be calculated from any two known points on the no-load saturation curve.

In deriving the equations, it was assumed that the air-gap deflection was a static deflection resulting from such causes as misalignment of the rotor in the stator, or the stator being out of round. The same type of analysis may be applied to a deflection that rotates with the rotor, and the same equations for unbalanced pull will apply.

The effect of parallel circuits in the rotor has been neglected, but it should be noted generally that with either a static deflection or a rotating deflection at a speed less than synchronous, the pull will be further reduced below the calculated values.

A two-pole machine will have less unbalanced pull than calculated for certain conditions of operation, but the formulas given should still be used to calculate the worst possible condition.

A two-parallel stator winding has been taken as the same as a series winding because a deflection along the line between the parallels will produce no compensating currents, and the pull will not be reduced. However, it should be noted that the pull will be reduced below the calculated value if the parallels of the different phases are spaced symmetrically around the machine or if the deflection is at right angles to the line between the parallels.

It is apparent then that the formulas given are either correct or somewhat pessimistic for all conditions of operation that could be considered as normal. Special cases of unbalance from such causes as short-circuited coils cannot be treated in a general method and should be solved individually as they arise.

Appendix I

Referring to Figure 2, assume a constant gap density over a section of the machine, and consider that a constant change is made in the air gap while the magnetizing current is kept constant.

$i_g = CK_c g$ and $i_g' = CK_c'(g-\Delta)$

$$i_g' = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,i_g$$

$$AB = ED = i_g - i_g' = \left(1 - \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\right) i_g$$

But $i_g = eI_g$ and $i_s = e^m I_s$

$$i' = i_g' + i_s' = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,eI_g + e^m I_s$$

$$\frac{di'}{de} = \cot\theta = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,I_g + me^{m-1}I_s$$

$$\frac{BC}{AB} = \tan\theta = \frac{1}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s}$$

$$BC = \frac{\left(1 - \dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right) eI_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s}$$

$$\frac{BC}{e} = \frac{b}{B} = \frac{\left(1 - \dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right) I_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s}$$

$$b = \frac{\left(1 - \dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right) eB_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g} + me^{m-1}\dfrac{I_s}{I_g}}$$

Appendix II

Referring to Figure 3, assume a constant gap density under an independent section of the winding (a parallel), and assume that the gap is changed a constant amount over this section. The most pessimistic condition is obtained if in Figure 4 we assume that the secondary winding is open-circuited.

As proved previously:

$$i' = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,eI_g + e^m I_s$$

$$\frac{di'}{de} = \tan\theta = \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\,I_g + me^{m-1}I_s$$

$$\tan\phi = \frac{i_m X_1}{i_m} = X_1 \quad \tan\gamma = \frac{1}{X_1}$$

$$AB = \left(1 - \frac{K_c'}{K_c}\,\frac{g-\Delta}{g}\right) i_g = h\tan\theta + h\tan\gamma$$

$$h = \frac{AB}{\tan\theta + \tan\gamma}$$

$$= \frac{\left(1 - \dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\right) eI_g}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}\,I_g + me^{m-1}I_s + \dfrac{1}{X_1}}$$

$$\frac{h}{e} = \frac{b}{B} = \frac{1 - \dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g}}{\dfrac{K_c'}{K_c}\,\dfrac{g-\Delta}{g} + me^{m-1}\dfrac{I_s}{I_g} + \dfrac{1}{I_g X_1}}$$

If a small deflection is considered

$$b = \frac{\dfrac{\Delta}{g}\,eB_g}{1 + me^{m-1}\dfrac{I_s}{I_g} + \dfrac{1}{I_g X_1}}$$

Appendix III

Let there be n parallels total in each phase

Angular spread of each parallel $= 2\pi/n$ radians.

In Figure 5 the equation of curve JKL is $K_s(\Delta/g)B\cos\theta$. The limits on θ for the rth parallel are $(r-1)2\pi/n$ and $r2\pi/n$. The average value of JKL over the rth parallel $=$

$$\frac{1}{\dfrac{2\pi}{n}} \int_{(r-1)\frac{2\pi}{n}}^{r\frac{2\pi}{n}} K_s\frac{\Delta}{g}B\cos\theta\,d\theta$$

$$= \frac{n}{2\pi}K_s\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Similarly, the average value of GHI over the rth parallel

$$= \frac{n}{2\pi}K_R\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

The drop in density under the rth parallel from the curve JKL to the final position at POQ

$$= (K_s - K_R)\frac{n}{2\pi}\,\frac{\Delta}{g}B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Equation of $POQ =$

$$K_s\frac{\Delta}{g}B\cos\theta - (K_s - K_R)\frac{n}{2\pi}\,\frac{\Delta}{g}\times B\left[\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right]$$

Assuming a large number of poles compared to the number of parallels and that part of the saturation is in the teeth of the machine, it may be readily proved that the pull along the line AB

$$= \frac{0.01386}{\pi}\left(\frac{4}{3}\right) BbA\cos\theta\,d\theta$$

For the rth parallel:

$$b = \frac{\Delta}{g}B\,[K_s\cos\theta - (K_s - K_R)\mathrm{B}]$$

where

$$\mathrm{B} = \frac{n}{2\pi}\left\{\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right\}$$

Total pull of the rth parallel along the line AB

$$= \frac{0.01386}{\pi}\left(\frac{4}{3}\right) B^2\frac{\Delta}{g}A \int_{(r-1)\frac{2\pi}{n}}^{r\frac{2\pi}{n}} \{K_s\cos^2\theta - (K_s - K_R)\beta\cos\theta\}\,d\theta = 1.84\times10^{-4}e^2\frac{B_g^2 A}{g}\times$$

$$\left[\frac{\sin r\dfrac{4\pi}{n} - \sin(r-1)\dfrac{4\pi}{n}}{4}K_s + (K_R - K_s)\frac{n}{2\pi}\times \left\{\sin r\frac{2\pi}{n} - \sin(r-1)\frac{2\pi}{n}\right\}^2 + \frac{\pi}{n}K_s\right]$$

Characteristics and Applications of Selenium-Rectifier Cells

E. A. HARTY
MEMBER AIEE

Synopsis: The rectification properties of selenium cells were first discovered in the year 1883. C. T. Fritts described them in the *American Journal of Science*.[11,12] However, they were never used to any extent, except possibly as photocells.

After the introduction of the copper-oxide rectifier, research activities were stimulated, and selenium cells were again rediscovered. The first commercial cells were made in Germany in the early '30's, and, as the technique of manufacture improved, better cells were made with better life expectancy.

The General Electric Company, after a period of developmental activity, started to make cells in 1938, first in its research laboratories, and subsequently a manufacturing plant was set up which permits producing cells in large quantities within relatively close electrical tolerances.

This paper contains data pertaining to these cells particularly, and the information may not apply in detail to cells manufactured by other methods without some correction factors.

Construction

THE selenium rectifier cell consists essentially of a carrier plate made of either aluminum or iron, supporting on one side a very thin film of specially treated selenium. The adhesion is quite intimate to prevent contact losses. This selenium film is given a series of controlled heat treatments to obtain a suitable crystalline structure.

Finally, a low melting point alloy is metal-sprayed onto the selenium surface. This layer is known as the "counterelectrode."

By means of subsequent electrochemical processes a film or blocking layer is formed between the counterelectrode and the selenium surface. Current flows freely between the selenium and the counterelectrode and is practically blocked in the other direction. Figure 1 shows a typical cross section of a cell.

Theory

Several theories have been suggested to explain the action of rectifiers of the type under discussion. The physicists do not all agree, and, therefore, no attempt will be made to discuss all these theories in detail.

One of the theories that appears the most logical to understand and that applies to all types of metal rectifiers is the following:

Metal rectifiers consist essentially of a semiconductor and a good conductor separated by a barrier or blocking layer which is, in itself, an insulator but through which electrons can pass in either direction. In the selenium rectifier the selenium layer is the semiconductor and the sprayed-metal counterelectrode the good conductor, the barrier or blocking layer being formed between these two substances. The sprayed-metal layer has an abundance of free electrons, while in the selenium layer, which is a relatively poor conductor, the free electrons are quite small.

When the two electrodes are connected to a source of supply, the opposite polarities set up an electric field across the barrier or blocking layer. Since this layer is very thin, a comparatively small electromotive force will produce a steep potential gradient. If the sprayed metal

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E. A. HARTY is design engineer, Tungar and metallic rectifier section, lighting engineering division, River Works, General Electric Company, Lynn, Mass.

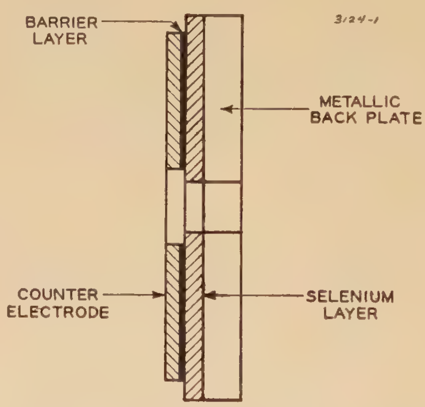

Figure 1. Cross section of a selenium cell

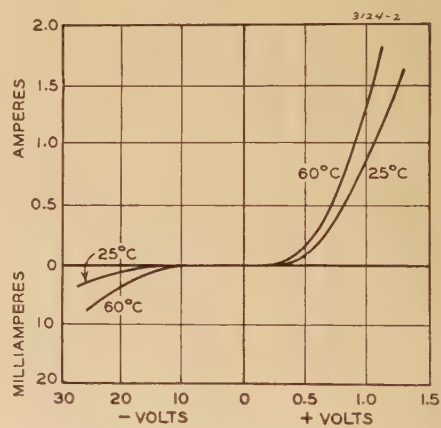

Figure 2. D-c characteristics

Appendix IV. Nomenclature

A—Area of the stator bore in square inches.

g—Single air gap in inches.

g'—Single air gap with he rotor displaced $(g-\Delta)$ in inches.

Δ—Amount the rotor is displaced from the center of the stator in inches.

K_c—Carter's coefficient for an air gap g.

K_c'—Carter's coefficient for an air gap g'.

B_g—Air-gap flux density at normal voltage in kilolines per square inch.

B—Air-gap flux density at any voltage.

b—Rise in the gap flux density.

e—Voltage in per unit corresponding to air-gap flux.

E—Per unit voltage at which maximum pull occurs.

I_g—Magnetizing current for the air gap at normal voltage.

I_s—Magnetizing current for saturation at normal voltage.

i_m—Magnetizing current at any voltage.

X_1—Stator leakage reactance in per unit.

X_m—Magnetizing reactance in per unit.

C_1—Form factor of the no-load field form of a synchronous motor.

m—Saturation curve exponent defined by $i_s=e^m I_s$.

K_s—Factor allowing for the effect of saturation on density rise.

K_R—Factor allowing for the effect of saturation and primary reactance on density rise.

a—Factor depending on the number of parallels.

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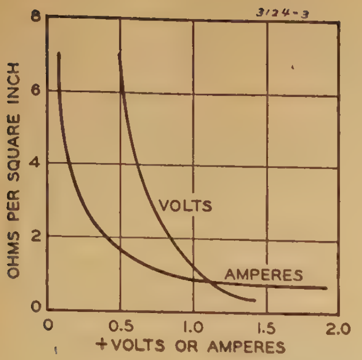

Figure 3. Resistance change with voltage or current

is connected to the negative pole of the source, the free negative electrons are accelerated to a sufficient velocity to enable them to pass through the barrier layer and the intercrystalline spaces of the selenium and reach the metal supporting disk, with the result that a flow of electrons is established which constitutes a current of electricity in the forward direction. When the polarity is reversed, the same action takes place in the opposite direction, but, since the number of free electrons in the semiconducting selenium is less than in the metal disk, the resulting current is much smaller. Because of this asymmetrical property, it is possible to rectify alternating current.

Electrical Characteristics of the Elements

Because it is almost impossible to make all cells exactly alike as to characteristics, even though materials and manufacturing methods are held within very close limits, the data that follow and also all published curves should be considered as representing average conditions. A slight deviation should be expected between individual cells.

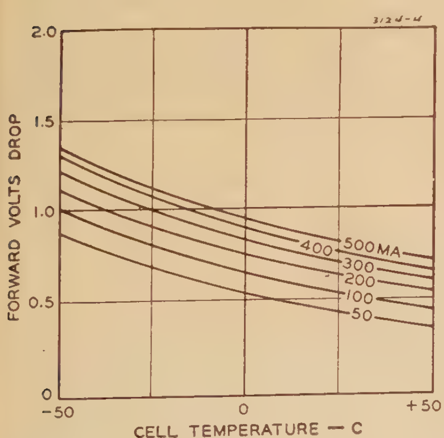

Figure 4. Temperature–forward voltage characteristics

One square inch area

D-C Characteristics

If a d-c voltage of varying potential is impressed first in the blocking direction and then in the flow direction and readings taken of the current that flows, a curve as shown in Figure 2 is obtained. This curve is based on selenium cells having one square inch of effective rectifying area.

In a similar way Figure 3 shows resistance versus voltage and resistance versus current. It should be noted and remembered that these curves are nonlinear and that they do not obey Ohm's law.

Effect of Temperature

The selenium rectifier cells have negative temperature coefficient which results

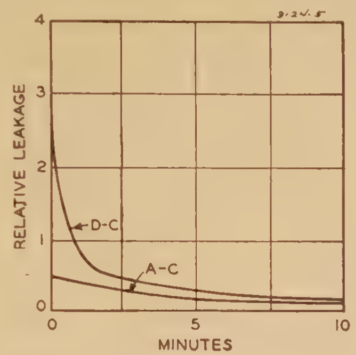

Figure 5. Leakage–time characteristics

in current increasing for a given voltage as the temperature goes up and decreasing as the temperature goes down.

Figure 4 shows this relation.

It should be noted that temperature also affects the leakage current in the blocking direction as shown on Figure 2.

Because of this characteristic, ratings should be such that the losses caused by heating should be kept under control; otherwise they may keep on adding, resulting in overheating and the eventual destruction of the rectifying film. In general, it may be stated that the rectifier will operate satisfactorily in a range of ambient temperature from −50 to +50 degrees centigrade.

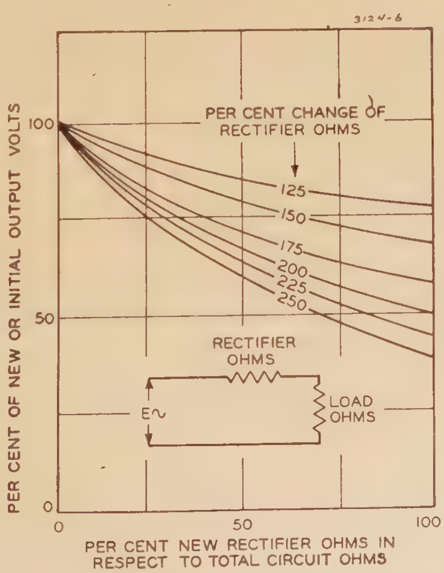

Figure 6. Effect of changing rectifier resistance on output

Forward Characteristics and Stability

The forward characteristic is very stable when a-c or d-c voltage is applied in this direction. However, as the cell heats up, changes occur. Over a period of time, the resistance of the cell appears to change and take a set. The rate of change increases with cell temperature, and at 100 degrees centigrade the cell is damaged.

An increase in forward resistance with time means that the difference between the input voltage and the output voltage will become greater. Therefore, to maintain a given output voltage constant, it is necessary to increase the input voltage. Great care must be taken to rate correctly these rectifier cells when new to prevent overloading in voltage after aging takes place.

Reverse Characteristics and Stability

The reverse or leakage characteristic on alternating current is quite good; it is, in general, higher initially but creeps to a minimum value in about two to three minutes. However, when a selenium rectifier cell is subjected to a d-c voltage in the inverse direction, the leakage current is very high initially. A polarizing action similar to that observed with electrolytic capacitors takes place, resulting in a steady decrease in current with time. Typical leakage voltage–time character-

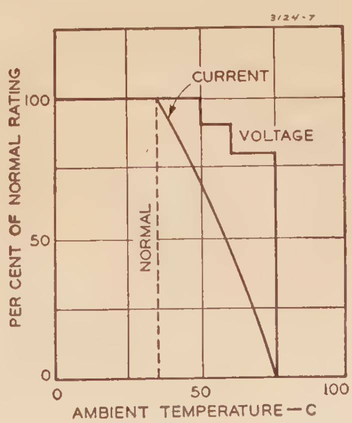

Figure 7. Rating of cells at high ambients

istics are shown on Figure 5. When the cells are continuously de-energized over a period of time, the reverse leakage resistance tends to decrease but will rapidly increase after the cells are again energized. It should again be noted that this same type of characteristic is also present in electrolytic capacitors. This characteristic should be borne in mind when applying these cells in blocking circuits, especially if instantaneous operation is required such as in certain types of high-speed relaying circuits.

Capacitance

A certain capacitance exists because of the presence of the barrier or blocking layer between the two electrodes of the rectifier cell. Measurements indicate this to be of the order of 0.02 microfarad per square centimeter. This may vary somewhat, depending on the past history of the cell.

In high-frequency applications, the capacitance acts like a shunt acrosss each cell, resulting in lowering the leakage resistance and changing the ratio of the forward to the reverse resistance. At normal a-c frequencies up to 2,000 cycles, the capacity effect usually can be disregarded.

Rectifier Circuits

Selenium rectifier cells are readily combined into series and parallel groups, depending on the voltage and current output required. These are used in rectifier circuits to change alternating currents to direct currents. The choice of the particular rectifier circuit rests with the designer. In view of the fact that rectifiers are used by many engineers who are not familiar with these circuits, a tabulation of the more popular circuits is included with this paper. (See Figure 19.)

It should be noted that these data apply to perfect sine waves, perfect rectifiers having no internal resistance and also no leakage whatsoever in the blocking direction. The load is based on a noninductive resistance.

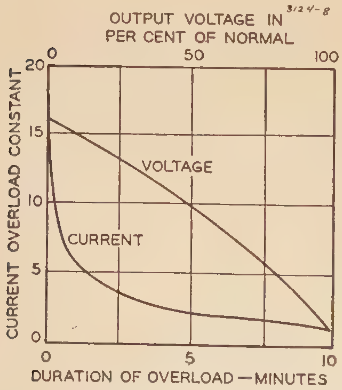

Figure 8. Overload rating of selenium cells

Calculations made with the aforementioned data are only of theoretical use and cannot be applied to actual design work without using correction factors.

Selenium rectifier cells have a fundamental resistance characteristic; this resistance is not constant and varies with current, temperature, and also time. Therefore, computations arrived at from formulas are not absolute, unless all these variables are considered. Design work should be based on empirical data based on years of experience to avoid trouble. Any correction factors must also have their own correction factors, since these will vary with time, temperature, and current.

In designing a practical selenium rectifier circuit, it is evidently very essential

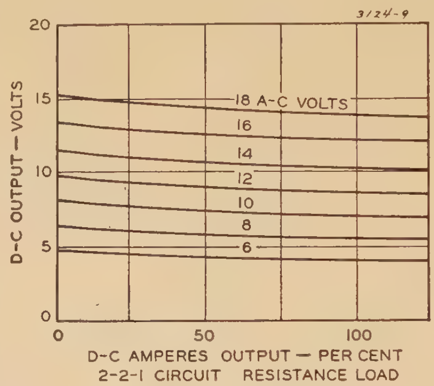

Figure 9. A-c to d-c characteristics of single-phase unit bridge rectifier with resistance load

One square inch area, 25 degrees centigrade

to pay particular attention to the fact that the rectifier resistance is not constant and changes with time, current, and temperature.

To minimize its effect on the rectifier circuit, it is quite essential to make the rectifier resistance small as compared to the total circuit resistance.[4] This is shown graphically in Figure 6.

The recommended ratings have been so chosen as to make the rectifier resistance about 10 to 15 per cent of the circuit resistance.

By referring to Figure 6, it can be noted that even though the rectifier resistance may double, the effect on the circuit is small.

However, when rectifiers are overloaded, the rectifier resistance in effect becomes a larger percentage of the circuit resistance, and, therefore, more changes

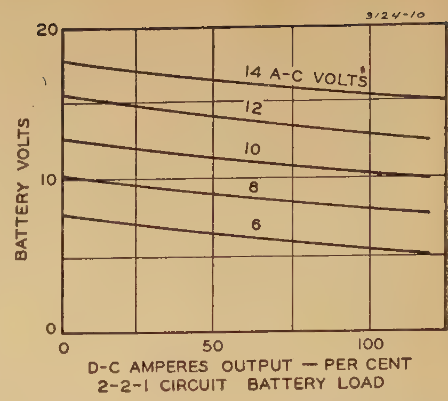

Figure 10. A-c to d-c characteristics of single-phase unit bridge rectifier with battery load

One square inch area, 25 degrees centigrade

should be expected in the output as readily shown in Figure 6.

Maximum Ratings

Table I and II show the voltage and current ratings for the various size cells which are available at the present time.

The circuit names as used by the metallic rectifier industry have been retained in Tables I and II.

The circuit symbolic notation is an attempt to overcome the objections raised to the circuit names by replacing them by a symbolic equation 3.

The first digit is the reciprocal of the fraction of time a cell carries current during the cycle. The second digit shows the cells in series and the third digit the cells in multiple carrying current instantaneously. This notation applies to unit rectifiers.

These ratings are based on the usual limiting factors of temperature rise, aging, and voltage breakdown. The in-

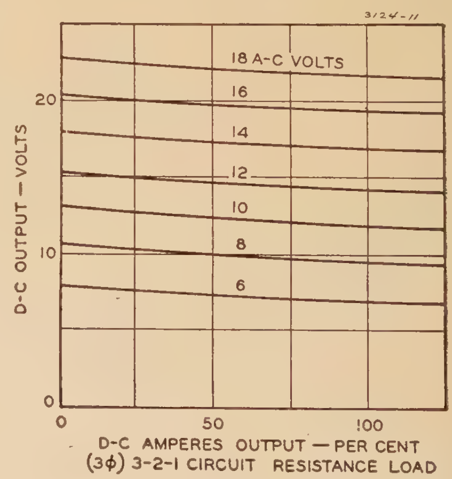

Figure 11. A-c to d-c characteristics of three-phase rectifier, bridge type, with resistance load

One square inch area, 25 degrees centigrade

Table I. Rating as Valves at 35 Degrees Centigrade

Code letter	F	A	C	H
Diameter of cells	1 Inch	$1^1/_2$	$2^3/_{16}$	$4^3/_8$
Inverse rms volts	18	18	18	18
Blocking volts d-c	15	15	15	15

ternal losses heat the rectifier cell, and the final permissible total temperature limits the output rating. It should be borne in mind that aging increases the heating, and, therefore, it is very important to run the rectifier cells somewhat cooler when new. This factor has been taken into consideration in the published ratings. The relation of cell spacing to cell diameter in a rectifier-stack assembly has been chosen for optimum cooling.

Permissible Temperature Rise

Although selenium rectifier cells may be operated up to a total temperature of 75 degrees centigrade, the ratings given in the previous paragraph are based on an ambient temperature of 35 degrees centigrade allowing a maximum temperature rise of 40 degrees centigrade. However, if the normal temperature is liable to be exceeded, the full-load ratings must be changed and reduced to prevent the total temperature from exceeding 75 degrees centigrade.

Figure 7 shows the recommended practice.

Overload and Intermittent Rating

Selenium rectifier cells will withstand short-time current overloads beyond the normal current, provided the cell is not heated above 75 degrees centigrade. If the cell is allowed to cool back to normal between loading periods, higher current overloads can be applied than in the case of insufficient cooling periods.

Figure 8 shows permissible current overload data and also how the voltage drops as the current increases.

Voltage overloads are not permissible, even for short periods, because of the danger of breaking down the blocking layer.

If the breakdown current is limited, the punctured cells sometimes self-heal. However, every healed spot robs the rectifying surface of some cross section, resulting in increasing effective resistance of the cell.

Unit Rectifier

For convenience in designing rectifiers, data have been prepared on a unit rectifier. This can be defined as any rectifier circuit having one cell in each arm. By reducing all measurements to a unit rectifier, it is possible to obtain general data that will apply to any rectifier, providing the number of cells in series in each arm is known. These same data can also be applied where two or more unit rectifiers are operated in multiple.

A-C–D-C Characteristics of a Single-Phase Full-Wave Unit Bridge Rectifier With a Resistance Load

By impressing various voltages across a unit bridge rectifier, a group of curves may be obtained as in Figure 9, showing d-c voltages plotted against d-c amperes output for various impressed a-c voltages.

A-C–D-C Characteristics of a Single-Phase Full-Wave Unit Bridge Rectifier as a Battery Charger

If the resistance load is replaced by a battery load, and data as shown previously are again repeated, a family of curves as shown in Figure 10 is obtained.

A-C–D-C Characteristics of a Three-Phase Full-Wave Unit Bridge Rectifier

If a three-phase rectifier is operated at different a-c voltages and different loads, a family of curves as shown in Figure 11 is obtained.

A-C Volts Drop Within a Unit Bridge Rectifier

If a d-c ammeter is used as a load across a unit bridge rectifier and the a-c input is varied, a curve as shown in Figure 12 is obtained. This permits calculating the a-c input voltage provided correction factors are used, depending on the circuit and its form factor.

Efficiency

Because of the presence of an a-c component in the d-c output of all rectifiers, it is quite essential to use the correct type

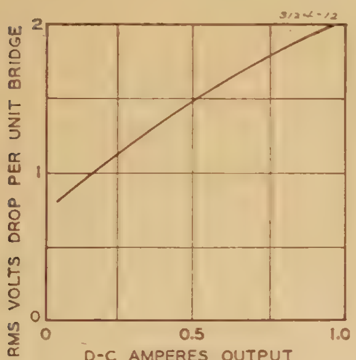

Figure 12. Rms volts drop in a unit bridge single-phase rectifier at different values of current

One square inch area, 25 degrees centigrade

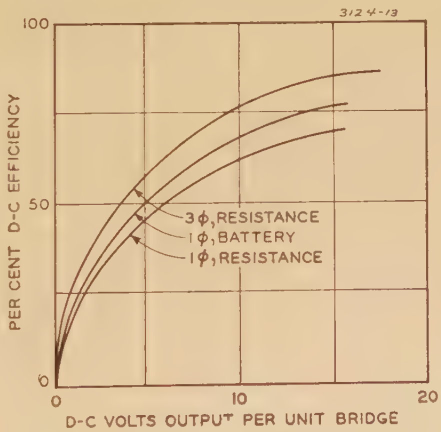

Figure 13. Efficiency of selenium cells at full-load current

Full-load amperes, 25 degrees centigrade

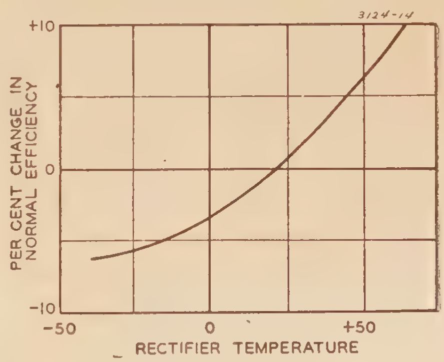

Figure 14. Correction curve to obtain efficiency at different temperatures

Table II. Current Ratings at 35 Degrees Centigrade for Resistance and Inductance Loads

Circuit	1 Inch	$1^1/_2$ Inches	$2^3/_{16}$ Inches	$4^3/_8$ Inches	D-C Volts	Circuit Symbolic Notation
One-phase half-wave	0.075	0.2	0.500	2.15	6	1-1-1
One-phase bridge	0.150	0.4	1.00	4.3	12	2-2-1
One-phase center tap	0.150	0.4	1.00	4.3	6	2-1-1
Three-phase half-wave	0.200	0.5	1.25	5.3	8	3-1-1
Three-phase bridge	0.220	0.600	1.4	6.5	16	3-2-1
Three-phase center tap with no interphase coil	0.270	0.7	1.8	8.0	8	6-1-1
Three-phase center tap and interphase coil	0.400	1.0	2.5	11.0	8	3-1-2
D-c valves	0.120	0.320	0.80	3.0	15	1-1-1

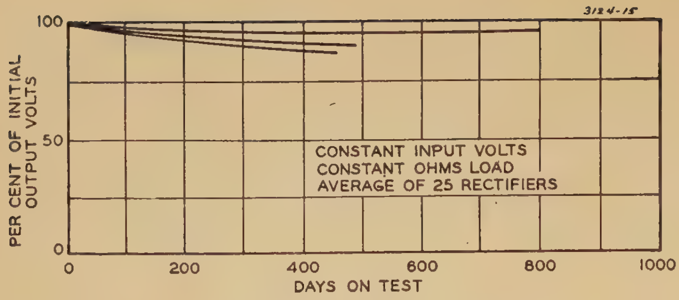

Figure 15. Aging of selenium rectifiers

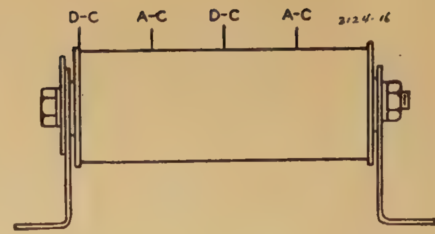

Figure 16 (right). Full-wave single-phase rectifier-stack assembly

of instruments to measure the power output.

The input is always measured with an a-c wattmeter and reads all of the power delivered to the rectifier.

If the nature of the load is such that both the a-c and d-c components of the rectifier output produce useful work, then an a-c wattmeter in the output measures the correct output. Examples of this type of load are resistances, lamps, radio-tube filaments, electromagnets, series motors, and so forth.

However, if the a-c component is not utilized, then the output should be measured with a d-c voltmeter and d-c ammeter of the D'Arsonval type. For example, battery charging, plating work,

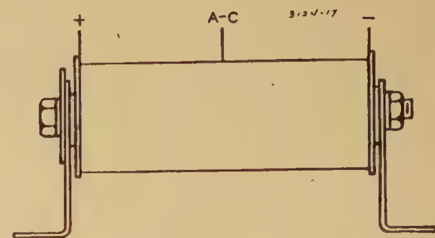

Figure 17 (right). Half-wave three-terminal-type rectifier-stack assembly

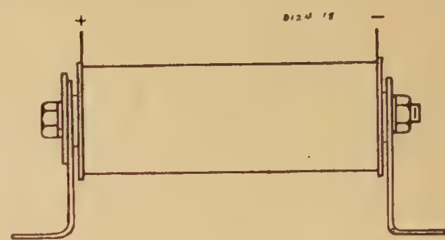

Figure 18 (right). Half-wave two-terminal-type rectifier-stack assembly

Figure 19 (below). Fundamental rectifier circuits

Symbolic Notation	Circuit	Output Wave Values: Wave Form	E_{avg}	E_{eff}	Form Fact.	D-C Ripple %	Rectifier Cell-Wave Values: Wave Form	I_{avg}	I_{eff}	Form Fact.	E Inverse	Trans. Capacity: Pri.	Trans. Capacity: Sec.
1-1-1	ERMS EAVG	1∿	0.45 E_{rms}	0.707 E_{rms}	1.57	121	½∿	1.00 I_{dc}	1.57 I_{dc}	1.57	1.414 E_{rms}	3.49 EI_{dc}	3.49 EI_{dc}
2-1-1	ERMS EAVG	1∿	0.900 E_{rms}	1.0 E_{rms}	1.11	48	½∿	0.500 I_{dc}	0.786 I_{dc}	1.57	2.828 E_{rms}	1.235 EI_{dc}	1.75 EI_{dc}
2-2-1	ERMS EAVG	1∿	0.900 E_{rms}	1.0 E_{rms}	1.11	48	½∿	0.500 I_{dc}	0.786 I_{dc}	1.57	1.414 E_{rms}	1.235 EI_{dc}	1.235 EI_{dc}
3-1-1	ERMS EAVG	1∿	1.17 E_{rms}	1.19 E_{rms}	1.02	21	$\frac{1}{3}$∿	0.333 I_{dc}	0.587 I_{dc}	1.76	2.45 E_{rms}	1.508 EI_{dc}	1.508 EI_{dc}
4-1-1	ERMS EAVG	1∿	1.27 E_{rms}	1.28 E_{rms}	1.005	11	$\frac{1}{4}$∿	0.250 I_{dc}	0.502 I_{dc}	2.01	2.828 E_{rms}	1.116 EI_{dc}	1.58 EI_{dc}
6-1-1	ERMS EAVG	1∿	1.350 E_{rms}	1.351 E_{rms}	1.001	4	$\frac{1}{6}$∿	0.167 I_{dc}	0.408 I_{dc}	2.45	2.828 E_{rms}	1.28 EI_{dc}	1.81 EI_{dc}
3-1-2	ERMS EAVG	1∿	1.170 E_{rms}	1.170 E_{rms}	1.001	4	$\frac{1}{3}$∿	0.167 I_{dc}	0.293 I_{dc}	1.76	2.45 E_{rms}	1.068 EI_{dc}	1.51 EI_{dc}
3-2-1	ERMS EAVG	1∿	2.340 E_{rms}	2.341 E_{rms}	1.001	4	$\frac{1}{3}$∿	0.333 I_{dc}	0.579 I_{dc}	1.74	2.45 E_{rms}	1.047 EI_{dc}	1.047 EI_{dc}

Figure 20. Typical selenium rectifiers for industrial applications

chemical work, electrolysis, shunt motors, and so forth. Therefore, the efficiency can be expressed as either

$$\text{RMS efficiency } \% = \frac{\text{a-c watts output}}{\text{a-c watts input}} \times 100$$

or

$$\text{D-c or average efficiency } \% = \frac{\text{d-c volts} \times \text{d-c amperes}}{\text{a-c watts input}} \times 100$$

In polyphase work, the rms efficiency approximately equals the average efficiency.

However, in single-phase work, there is a difference of approximately 15 points in efficiency between the average and rms values, the latter being the highest.

Typical efficiency curves are shown in Figure 13 at different voltages and at full-load current densities. Figure 14 shows correction factors at different temperatures.

Life Tests

General Electric selenium rectifiers have undergone extensive tests and show promise of better aging characteristics than some of the European elements previously tested.[4] Typical life tests are shown in Figure 15. It should be noted that these tests represent aging as seen from the user's viewpoint.

Practical Rectifier Design

It is believed that a worked-out example will show how to use the data contained in this paper.

For example, assume that we want a 30-volt rectifier at one ampere working on a resistance load from a single-phase circuit. By reference to the tabulation on ratings, we find that the most voltage we can obtain from a unit bridge rectifier at full current density for each type cell is 12 volts. Dividing $30/12 = 2.5$, therefore, three is the minimum number of series cells to be selected.

The voltage to be supplied by each unit bridge rectifier will be $30/3 = 10$. By reference to Figure 9, we find that, at full-load current and ten volts, we need 13.2 volts alternating current per bridge, or $3 \times 13.2 = 39.6$ for the rectifier under discussion.

The size of the cell can be determined by reference to the tabulation on ratings. To carry one ampere a type *C* cell having $2^3/_{16}$-inch diameter is required. If the current rating required does not match that of the four available sizes, then it will be necessary either to operate at slightly less than full rating or to use several smaller cells in multiple.

From the aforementioned calculations, to obtain 30 d-c volts at 1.0 ampere direct current, a bridge rectifier using three cells in series would be required. The a-c input is 39.6 volts, and the transformer should have sufficient taps to take care of line voltage variations. To take care of aging, it is advisable to tap the transformer up to 3×18 rms volts or 54 volts. The approximate efficiency is 63 per cent at 25 degrees centigrade as seen from Figure 13 and will be higher at the operating temperature as shown on Figure 14.

Construction of Selenium-Rectifier Stacks

Selenium rectifier cells are assembled into either full-wave or half-wave stacks, depending on their voltage and current rating. Low-voltage stack can generally be assembled into single stack, using a construction as shown in Figure 16.

For higher voltages, two stacks are sometimes used, embodying a construction as shown in Figure 17.

For still higher voltages four units are used as shown in Figure 18.

The cells are spaced to permit free ventilation on both sides, light spring washers being used to collect current.

The stacks are clamped under light pressure and come either with or without mounting brackets as shown in Figure 20.

Installation of Rectifier Stacks

Selenium rectifier stacks should be installed in well ventilated cabinets to permit free circulation of air.

They should be located preferably at the bottom of cabinets so that any heat from other heat dissipating apparatus does not have a cumulative effect.

If installed in closed cabinets, a certain amount of derating should be made to take care of the higher internal ambient temperatures as previously explained.

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A New High-Interrupting-Capacity Fuse for Voltages Through 138 Kv

H. H. FAHNOE
ASSOCIATE AIEE

Synopsis: The reliable performance of high-interrupting-capacity fuses in the intermediate voltage field has led to an increased interest in the application of such fuses for the higher voltages. Unfortunately, a fuse for the higher voltages cannot be made by simply enlarging or lenghthening the lower-voltage devices. To interrupt the higher currents in a reasonable structure, arc lengths must be kept short to reduce mechanical stresses. Interruption in these short distances creates high dielectric stresses external to the fuse that would endanger the operation of a device built along conventional lines. Adequate conductors for carrying the higher currents through the longer-length fuse must be moved quickly during interruption.

A new high-voltage fuse in which interruption of the higher currents is accomplished in a short length without undue voltage stresses adjacent to the fuse has been developed. This fuse has a new arrangement of conductors which permits rapid extension of the arc without expulsion of any conductor parts from the fuse. All this has been accomplished with a fuse of such light weight that it permits the use of a drop-out design which is a necessity at the higher voltages to prevent subsequent flashovers caused by leakage currents. The interrupting medium is compressed boric acid which has proved very effective and reliable at the lower voltages. Repeated interrupting and mechanical tests have demonstrated the suitability of the new fuse for service on important transmission circuits.

PRESENT day requirements relating to the transmission of large blocks of electrical energy to large industrial areas and new war industry plants remote from power generation plants demand short-circuit protective devices that will afford the maximum protection with a minimum outlay of critical materials. The excellent operating record established by modern high-interrupting-capacity power fuses in the intermediate voltage field has created an insistent demand for fuses suitable for application on transmission systems in the 44-kv to 132-kv class. Power fuses require less critical material than a conventional hook-stick-operated disconnect switch and can be mounted in the same space, thus effecting economies in substation space and structure. Certain operating characteristics of high-capacity power fuses provide for the maximum continuity of service so essential to modern production methods. Power fuses when applied for the short-circuit protection of transformers or circuits provide ultrahigh-speed clearing of faults. Faulted equipment is isolated from the system in a small fraction of the time required by conventional short-circuit protective devices, thus greatly reducing the damage to the equipment and enabling it to be repaired and returned to service at a much earlier date. Operating speeds equal to or less than the most modern ultra-high-speed circuit interrupters insure maximum system stability since the voltage dip associated with a fault persists for as short a period as one cycle. The short duration of voltage fluctuation insures that relays elsewhere on the system will not operate to stop important continuous process loads. The high-speed operation of power fuses reduces the requirements for elaborate relaying schemes on new transmission circuits and eliminates the need for extensive relay changes when new load centers are added to an existing transmission line.

Requirements for Higher-Voltage Power Fuses

Since electrical power circuits operating at 44 to 132 kv are intended for the transmission of large blocks of energy, it follows that such systems are connected to a generating source of considerable capacity, capable of delivering a correspondingly high value of short-circuit energy to a fault. Unlike conventional circuit breakers which require as long as eight cycles to interrupt a fault and are so rated, power fuses frequently operate in the first half cycle of a fault; hence they may have to interrupt a fully displaced or asymmetrical current. For the systems under discussion the rms value of this current may be as much as 1.6 times the symmetrical short-circuit current. This ratio of currents is dependent on the characteristics of the transmission circuit. The closer the fault is to the generating source the greater ratio of reactance to resistance and the higher the ratio of asymmetrical to symmetrical current which approaches the theoretical limit of 1.73. From this it can be seen that power fuses for use on high-voltage circuits must have an interrupting capacity considerably in excess of the symmetrical short-circuit current. To handle the most severe case a fuse for 1,000,000-kva systems must be able to interrupt at least 1.6 times the current value corresponding to a 1,000,000-kva symmetrical fault. It is obvious therefore that a fuse for use at the higher voltages

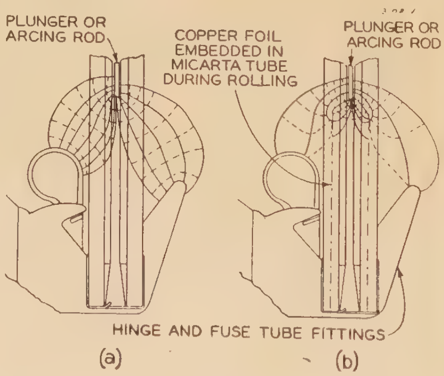

Figure 1. Approximate plot of dielectric field at lower end of fuse hit

Note how electrostatic shielding in diagram b reduces the lines of force or potential gradient at the lower fittings. The reduction of potential gradients at these points eliminates the possibility of external flashovers

(a). Without electrostatic shield

(b). With electrostatic shield

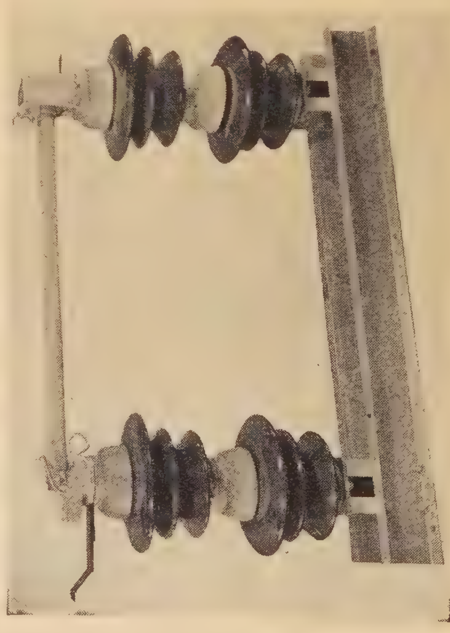

Figure 2. Single-pole unit, 92 kv, 200 amperes

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H. H. FAHNOE is design engineer for Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

must have a high interrupting capacity.

A second requirement is the ability to interrupt positively the entire range of fault currents from the minimum melting current of the fusible element up to its full interrupting rating. While such fuses are normally applied solely for short-circuit protection, it is recognized that certain types of high impedance faults may develop, transformer secondary protective devices may be inoperative, or that faults may develop in a period of light generating capacity, requiring interrupting at low values of fault current.

A further requirement, especially important at these higher circuit voltages, is that the restored voltage shall not overstress the insulating material, making up the fuse tube so that there are high leakage currents which may result in flashover or dielectric breakdown. Since circuits of this voltage are sometimes supplied from hydroelectric plants where the sudden dropping of a large load may cause "runaway" of the generators before protective relays operate, the restored voltage may rise to a value double the normal circuit voltage. These conditions make it imperative that the higher-voltage fuses be automatically removed from the circuit immediately after operating.

Additional requirements particularly important to enable operating personnel to locate blown fuses and replace these with new units are positive indication of blown fuses and a lightweight simple unit that can readily be removed and replaced in the fuse support.

Evolution of the New Fuse

An analysis of the characteristics of the dry-type high-interrupting-capacity fuses for operating voltages up to 34.5 kv reveals that there are four major design features which have been responsible for its exceptional performance record.

1. Boric acid in the dry molded form as the lining of the interrupting chamber will interrupt a much higher voltage per inch of length than horn fiber on other fuse-tube materials in an equivalent structure. Since this is

Figure 4. Fuse unit with disconnecting fittings

especially true in the lower range of currents, fuses utilizing boric acid can be made in a shorter length. At the higher currents this shorter-length boric-acid-lined fuse tube does not produce as vigorous a gas blast as the equivalent longer length of fiber; consequently, mechanical stresses in the tube structure are much less. In addition, the voltage gradient that can be interrupted increases with current much more rapidly. The gases evolved from boric acid are noncombustible and have a much higher dielectric strength than gases from organic materials and are much less liable to cause breakdown between adjacent line parts.

2. The very high interrupting capacity of this type of interrupter is largely due to the use of a very short fuse element in an enlarged opening of the fuse tube that is freely vented and the drawing of the arc into a more restricted section of the interrupting chamber. There is sufficient gas blast from the boric acid even in this enlarged opening to interrupt the higher currents at the end of the first half cycle of arcing without developing excessive pressures.

3. The use of a solid-rod-type conductor through the arcing chamber further increases reliability of high current interruptions, since there is no flexible conductor or cable to become jammed in the fuse tube by the gas pressure developed on very high fault currents.

4. Interruption of extremely high currents without excessive internal pressure and without severe voltage surges is further accomplished by the use of controlled mass and acceleration of the arcing terminal through the interrupting chamber. The mass of the arcing rod and of the operating spring and shunt assembly is so co-ordinated with the tension of the spring that the arcing rod is accelerated only a few inches or only a fraction of the length of the interrupter in the first half cycle. This short-length arc in the enlarged opening results in minimum mechanical stress in the fuse structure.

The first problem of the designer in converting low-voltage fuse structures to a design suitable for the higher voltage is to determine a suitable length of fuse. Since in the interests of simplicity of handling and replacement by operating personnel, it is desirable to have a disconnecting-type fuse, the minimum length of the fuse is automatically set by the standards for such types of equipment. The actual length of the fuse unit is then dependent upon the required length of interrupting chamber to secure efficient interruption throughout the entire current range. The voltage gradient that can be interrupted in a boric-acid interrupter varies with the diameter of the openings in the boric acid. Furthermore the voltage gradient is not constant for a given bore but varies with the length. For longer lengths, the voltage that can be interrupted corresponds to approximately the 1.7 power of the length. A large bore is required for in-

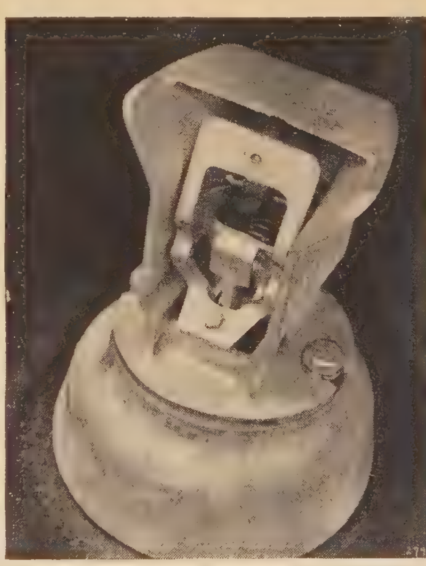

Figure 5. Sleet hood with dropout mechanism

Note the trigger which is actuated by the moving plunger to initiate the dropout action

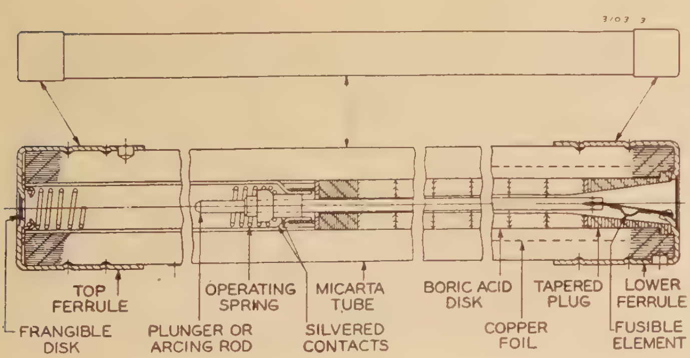

Figure 3. Cross section of new fuse unit

The unit for 138 kv is 72 inches long and 2 1/2 inches in diameter

terruption of the higher fault currents to minimize mechanical stresses; however, it would be impractical to build such high-voltage fuses with a constant bore as the length required to interrupt currents in the lower range would be excessive. To make the fuse as short as possible, a stepped or tapered bore is used with the largest opening in the vented end so that gases are readily vented on high-current shots and the bore gradually decreasing to a diameter just larger than the size of the conductor rod. In this way, the fuse can be made just as short as the permissible open-gap spacing for a disconnecting-type unit, since the lower currents are readily interrupted when the arc is drawn into the more restricted bore. The use of the conventional spring and shunt assembly is not practical in the higher-voltage fuse as the collapsed length of such an assembly is approximately one third the length of the entire fuse unit. Therefore, to make the fuse unit as short as desired, a new method of conducting current from the top ferrule to the solid arcing rod has been developed which occupies a length equivalent to the length of the interrupting chamber.

After the minimum length has been established, the next step in the evolution of the higher-voltage fuses is to determine the form of the fuse unit. From a weight and handling standpoint, the use of insulating tube of organic material with a porcelain weatherproof casing is at once ruled out. Since it is recognized that organic insulation, such as a fuse tube, should not be subjected to high-voltage stress, such as restored voltage, after the fuse has blown, a drop-out-type fuse is indicated.

The decision to make a drop-out-type fuse entails further design problems, notably the necessity for a lightweight construction to eliminate any shock to the insulator column supporting the pivot end of the long fuse tube. The use of a permanent fuse holder with a renewable element or refill as has been used with such success in the lower-voltage fuses must be eliminated to avoid duplication of structure. In a design incorporating a renewable refill, the refill unit must be mechanically strong enough to withstand the entire pressure developed in the interupting chamber during interruption of the maximum fault current and as the pressures in the refill are communicable to the fuse holder, the holder must be of equally sturdy construction. The decision to make the higher-voltage fuses in a single nonrenewable unit was aided by early tests on a refill- and holder-type unit. With the lengths involved for the higher voltages it is virtually impossible to maintain the extremely close clearances between the outside of the refill tube and the inside of the holder tube required to prevent flashover in this space. The presence of ionized gases within the holder and the high-voltage gradients present in this space on high current interruption were very conducive to flashovers in this space. As the length of the interrupter increases, the voltage that can be interrupted increases faster than the dielectric strength of the adjacent air gap.

With lightweight construction a paramount requisite, it is imperative that the interrupter be very efficient so that the mechanical stresses resulting from gas pressures developed do not require an excessively strong and heavy structure. The efficiency of an interrupter is based on building up high dielectric strength in the arc path at current zero with low arc voltage and correspondingly low arc energy. An efficient fuse design is based on drawing an arc as short as possible in a structure offering a minimum of actual restriction to the arc core and develop sufficient un-ionized gases to effect interruption at first current zero. The interruption of high currents in such a short length imposes very severe dielectric stresses between the end of the arcing rod and the lower terminal of the fuse with its associated fittings.

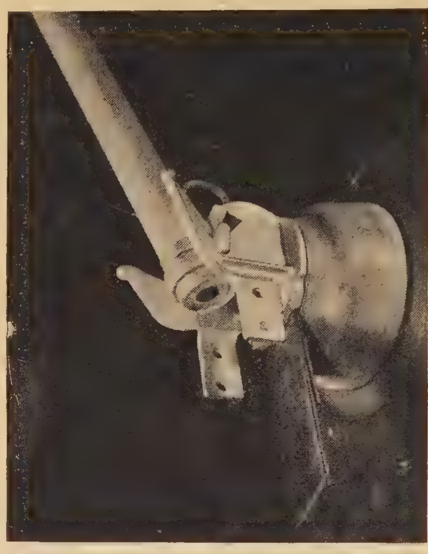

Figure 6. Hinge with fuse unit

To prevent external flashovers resulting from the presence of ionized air associated with such extreme voltage gradients, an electrostatic shield has been built into the tube structure. Figure 1*a* shows the voltage stress lines between the end of the arcing rod and the lower terminal. It is readily seen that very high voltage gradients are present at the fittings adjacent to the tube. The corona associated with such high-voltage stresses creates sufficient ionized air surrounding the tube to cause external flashover over the entire length of the fuse unit. Figure 1*b* shows the voltage-stress distribution with an electrostatic shield in the insulating tube. It will be observed that the maximum voltage gradient is now impressed on solid dielectric and not on the air adjacent to the fuse tube, so that external corona is eliminated.

A further requisite of the drop-out type of fuse construction, particularly for the voltages under consideration, is that the drop-out action must not start until the circuit is positively interrupted within the fuse unit. If the fuse unit should start to drop out before the current is interrupted, there will be arcing at the top contacts which would create sufficient ionized gases to cause flashover

To secure positive time delay between the time of fusion of the calibrated element and the initiation of the drop-out action, a construction has been adopted whereby the drop-out action is not initiated until the moving arcing rod has reached the end of its travel.

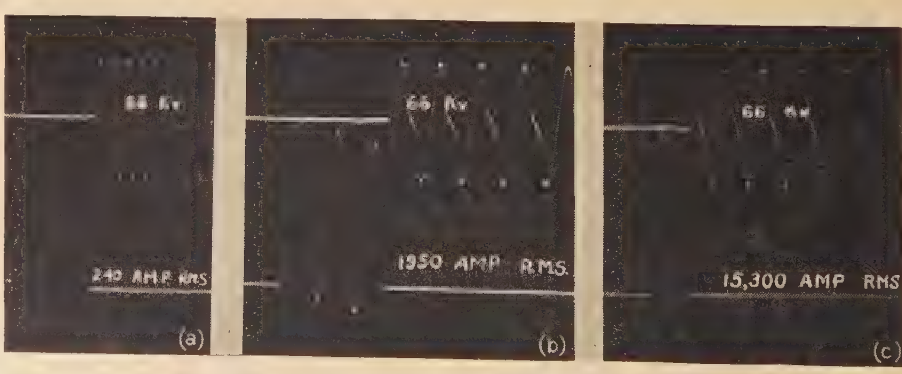

Figure 7. Oscillograms of interrupting tests on 69-kv fuse

(*a*). 240 amperes 66-kv restored voltage
(*b*). 1,950 amperes 66-kv restored voltage
(*c*). 15,300 amperes 66-kv restored voltage

Construction and Operation

A complete fuse consists of a fuse unit which is nonrenewable and is replaced by an entirely new unit after fault interruption and a fuse mounting which includes the necessary fittings to convert the fuse unit into a disconnecting unit. Figure 2 shows this assembly in the closed position.

The Fuse Unit

The new high-interrupting-capacity fuse unit consists of a short low-temperature calibrated element at the end of a solid conductor rod extending through a boric-acid-lined chamber, a copper tube for conducting current from the upper ferrule to the arcing rod through a modified form of tulip contacts and enclosing the operating spring which withdraws the arcing rod from the interrupting chamber, all enclosed in a lightweight Micarta tube with copper ferrules as shown in Figure 3.

The insulating tube is made from a rolled Micarta tube selected for high mechanical and impact strength, high dielectric strength, resistance to moisture absorption, and freedom from warping. The electrostatic shield at the lower end of the tube consists of copper foil rolled into the tube during fabrication in the same manner as used in the construction of high-voltage condenser bushings. This foil is electrically connected to the lower ferrule of the fuse unit. Copper ferrules are shrunk onto the fuse tube and then rolled.

The lower half of the fuse tube is lined with molded boric-acid blocks cemented in the tube with a moisture-resistant high-dielectric-strength cement. The diameter of the opening in the boric acid is a maximum at the bottom of the interrupting chamber tapering down to a diameter slightly larger than the size of the arcing rod at the top. The length of boric acid having maximum opening is chosen so that interruption of the maximum fault currents will take place when the arcing rod has traveled a distance sufficient to prevent severe voltage stress. Moderate fault currents are interrupted when the arcing rod has traversed the intermediate taper, and low currents are cleared when the arcing rod is drawn into the final taper. To insure that low fault currents corresponding to the minimum melting current of the lower rated fuse elements are cleared in the shortest possible arcing time, the arcing rod and the bore of the top section of boric acid are of smaller diameter than that of the rod and boric-acid blocks used for the high current ratings. The minimum current to be interrupted by a fuse will be, of course, dependent on its ampere rating.

An enlarged section at the top end of the arcing rod engages the flexible contact fingers which are part of the conductor tube to the top fuse-tube ferrule. The contact surfaces are heavily silver-plated for low contact drop for the life of the fuse under all atmospheric and temperature conditions. A stainless-steel compression spring assures uniform and continuous contact pressure without binding. These contacts are so biased that the effort required to withdraw the contact from the fingers is only a small fraction of that exerted by the stainless-steel operating spring which is terminated on this enlarged section of contact rod.

The calibrated element in this new fuse is of the low-temperature type so that operation at or near full-load rating will not create temperatures that will dehydrate the boric-acid lining or impair the strength of the fuse tube. A high-strength strain element relieves the calibrated element of all spring tension.

When the calibrated element is fused by a fault current, the operating spring draws the arcing rod through the boric-acid arcing chamber. The heat of the arc decomposes the surface of the boric acid into water vapor and inert boric oxide. The blast of water vapor deionizes the arc path and effects interruption. The arcing rod, which is not rigidly connected to the operating spring does not stop when the spring reaches its fully closed position but continues on through the spring by virtue of its inertia until the end punctures a frangible disk in the top ferrule. A special spring washer stops the travel of the arcing rod by arresting its enlarged section. This washer also functions to retain the arcing rod and prevent it from dropping back into the tube. The rod projecting from the top ferrule initiates the drop-out mechanism. It is obvious that the drop-out action cannot start until the arcing rod has traversed the entire length of the interrupting chamber which insures that all fault currents are interrupted before the tripping out action occurs.

The Fuse Mounting

The fuse-unit fittings which comprise

Figure 8. Oscillograms of short-circuit tests on 92-kv fuse

(a). 167 amperes, 88-kv restored voltage

(b). 3,600 amperes, 88-kv restored voltage

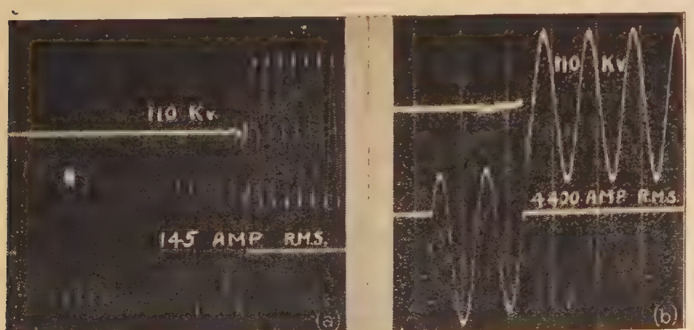

Figure 9. Oscillograms of short-circuit tests on 115-kv fuse

(a). 145 amperes, 110-kv restored voltage

b). 4,400 amperes, 110-kv restored voltage

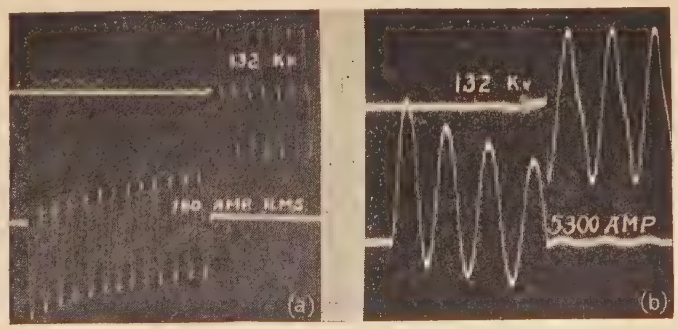

Figure 10. Oscillograms of short-circuit tests on 138-kv fuse

(a). 180 amperes, 132-kv restored voltage

(b). 5,300 amperes, 132-kv restored voltage

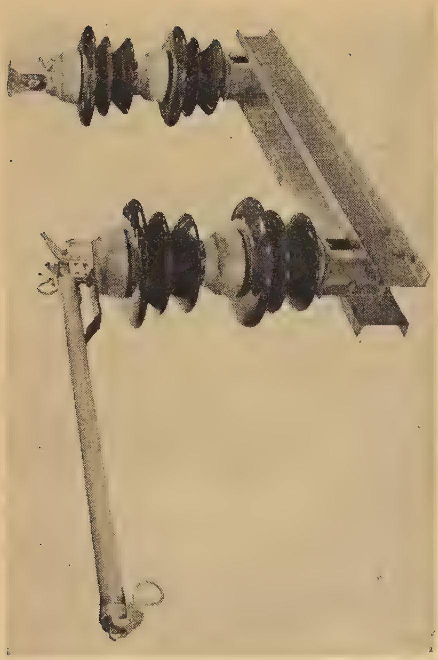

Figure 11. Single-pole unit, 92 kv, 200 amperes, with fuse. Unit in dropout position

a part of the fuse mounting are shown clamped onto the fuse tube in Figure 4. These fittings, comprising a hook eye for the top end of the fuse unit so that the fuse unit can be used as a disconnecting switch and a hinge casting for the lower end of the fuse tube that incorporates a lifting eye to facilitate the removal and replacement of the fuse with a standard hook stick, are readily removed from a blown fuse unit and placed on a new unit. These fittings are so keyed to the fuse unit that the correct fitting can fit only on its end of the fuse unit and assures perfect alignment of the fittings.

The fuse mounting proper consists of a double steel channel base for the higher voltages, and two insulator columns with latch castings and hinge castings mounted thereon. The top contact and trip-out mechanism are enclosed in a sleetproof housing, as shown in Figure 5. Current is conducted from the terminal to the pivoted latch casting by means of a flexible copper shunt. A powerful stainless-steel compression spring insures adequate contact between the latch casting and the hookeye casting clamped to the top ferrule of the fuse unit. It will be noticed that the force exerted by this spring puts the fuse tube in compression and thus avoids warping of the fuse tube from continuous cantilever stress. The drop-out trigger is pivotally mounted on the latch casting and bears on a projection within the sleet hood. Tests have shown that the kinetic energy of the moving arcing rod is more than ample to insure positive tripping regardless of alignment, variations in length of the fuse unit, or any possible friction that might be due to weathering or corrosive atmospheres.

The construction of the hinge is shown in Figure 6. Current is conducted from the fuse unit to the terminal pads by copper alloy leaf springs. The lower or lifting eye casting on the fuse unit and the hinge casting are so designed that the fuse unit is positively guided throughout the disconnecting or closing-in movements. The fuse unit can be removed from the mounting only when the fuse unit is in the 180°-open position. This arrangement also provides for the lower insulator stack taking the entire upward reaction resulting from gas blast on high current interruption thus insuring proper action of the drop-out mechanism. Below the hinge casting is a simple leaf spring bumper to absorb the energy of the falling fuse unit without overstressing the insulator column. This arrangement was chosen in preference to a friction brake because of its simplicity, its reliability under all atmospheric conditions, and the amount of energy to be absorbed in braking the fall of the long fuse unit.

Test Results

Complete interrupting tests have been made in the high-power laboratory covering the range from the minimum melting current of the smallest fusible element up to destruction of the fuse. Test-circuit conditions were modified from a very severe circuit where voltage-recovery rates correspond to a natural frequency of approximately 7,000 cycles per second to circuits with a very low voltage-recovery rate corresponding to natural frequencies as low as 800 cycles per second.

The majority of the interrupting tests were made with full line-to-line voltage impressed across a single fuse unit with the lower terminal at ground potential. Other tests were made with line-to-line voltage across a single fuse with the neutral grounded at the fuse mounting base. Also the fuses have been tested at line-to-neutral voltages to determine the upper current limit at reduced voltage.

Representative oscillograms of interrupting tests are shown in Figures 7 to 10.

The new assembly for carrying current through the fuse unit has been thoroughly tested for reliability on all values of fault current up to those which cause destruction of the fuse unit. In addition, high-current short-duration faults that did not blow the largest fuse rating were applied. In no case was there a failure of these parts to perform satisfactorily. In the case of the high currents applied momentarily there was no welding or damage that might cause failure to operate on subsequent faults. Prolonged temperature-rise tests indicate the complete adequacy of all current carrying parts and contacts. The silvered contacts on the fuse rod within the fuse unit carried full rated current with an unusually low temperature rise.

High-speed moving pictures were used extensively during interrupting tests as a check on the operating sequence of the drop-out action and for checking the time required to trip out. These moving pictures were of invaluable assistance in locating the cause of flashovers in the early designs. They also presented an accurate picture of the size and shape of the cone of gases evolved from the fuse, thus enabling the designer to design an opening so that gases would not be deflected in such a way as to cause flashover of adjacent apparatus.

Application

The selection of a high-voltage fuse for isolation of faults in potential transformers does not require any special precautions, as the fuse can operate only on a fault in the connected equipment. When high-voltage transmission system is tapped to obtain small blocks of power, it is recommended that high-voltage fuses be connected as close to the power transformer as practicable and that sec-

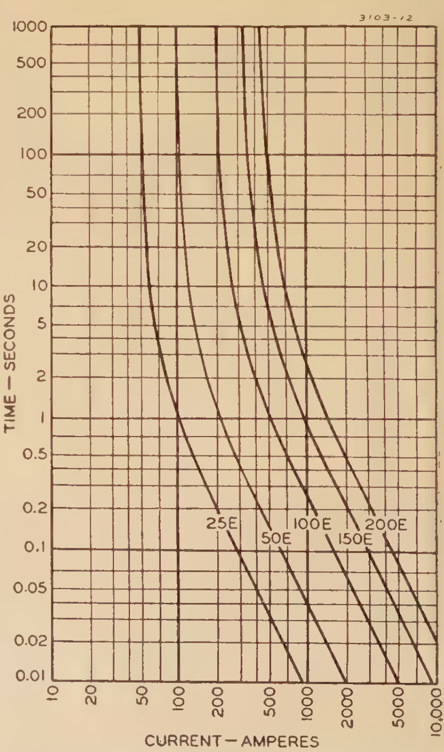

Figure 12. Melting time-current curves for representative fuse ratings

Subcenter Switching Systems for Teleprinter Tie Lines

F. L. CURRIE
MEMBER AIEE

Synopsis: One of the means used by the telegraph companies for improving the speed of their service is the provision of printing telegraph circuits between their central offices and the offices of many users of the telegraph. In the beginning, circuits of that type were restricted to patrons whose volume of telegraph business and proximity to a central office justified the expense involved in their installation and maintenance. A large part of that expense, when the patrons are located remotely from the central office, is in the line-wire costs.

This paper describes an automatic switching means for economically enabling a large number of patrons, when grouped in one locality, to be furnished printing telegraph service with a distant central office by causing them to share the use of a comparatively small number of line wires between that locality and the distant central office.

This paper also cites benefits gained from present installations.

AMONG the methods used by the telegraph companies during the last two decades for improving the speed of their service has been the provision of printing telegraph circuits between their central offices and the offices of many users of the telegraph service. That type of accommodation employes the use of the printing telegraph mechanisms, variously termed "teleprinters," "teletypewriters," or "teletypes," and is known in the Western Union Telegraph Company as teleprinter tie-line service.

Because of the expense involved in providing and maintaining the equipment and line wires essential to that service, it has been restricted to those patrons whose proximity to the central office and volume of business warranted the expenditure. Therefore, many substantial users of the telegraph service, because of their remoteness from a central office, had to be denied the advantages of teleprinter tie-line service because of the high line-wire costs involved. In some cases even groups of patrons were in that category.

The Western Union Telegraph Company undertook several years ago the development of a means for bringing those groups of remote patrons within the scope of teleprinter tie-line service by reducing the amount of line wire required.

Long experience in serving patrons over teleprinter tie-lines through central-office concentrators[1] has shown that most of those lines are in use only a small part of the time, and that the probability of any large percentage being used at any one time is small. That knowledge of their normal requirements led to the development of a remote controlled switching means for enabling a large number of patrons, grouped in one locality, to share the use of a comparatively small number of line wires to a distant central office. In that manner the necessary line-wire savings are effected to make economically feasible the extension to many more patrons the faster service afforded by teleprinter tie-lines.

The switching means, termed "subcenter," is designed to be installed in the same locality as the group of patrons, to be connected to each patron's office by an individual line wire, termed "patron's line," and to be connected to the central office by only as many line wires, termed "trunks," as the peak volume of business at any one time requires. An installation containing a subcenter is termed a "subcenter switching system."

Description

Equipment

The equipment required for a subcenter switching system is divided into

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F. L. Currie is an engineer at the Western Union Telegraph Company, New York, N. Y.

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ondary circuit breakers or other protective devices be used to interrupt all secondary faults. The high-voltage fuses will then operate only on faults in the power transformers. For such applications the fuse rating selected must provide adequate time for co-ordination with the relays controlling the secondary breaker. The proper rating should be determined from the time–current characteristic curves of the fuses and will usually exceed four times the full-load current of the transformer.

The addition of fuses on a system to isolate faults in equipment will enhance the reliability of the entire system by the rapid interruption of short circuits that would otherwise result in breaker operations or even in ground that might be difficult to locate.

At the higher transmission voltages it is generally not advisable to use fuses to protect even a small tap line that is exposed to lightning, unless the nature of the load is such that an outage caused by fuse operation can be tolerated.

Conclusions

Numerous interrupting tests, covering the complete range of fault currents under all possible circuit conditions, show that these new fuses are entirely satisfactory for application to important high-voltage transmission circuits. Their interrupting ability is such that they may be safely applied to systems approximating 1,000,000-kva short-circuit capacity at 66 kv through 132 kv.

The positive time-delay drop-out action provides an air gap in the circuit after operation which has a breakdown voltage ten per cent greater than the breakdown voltage of the basic impulse level (BIL) insulator columns. These fuses may therefore be applied on hydroelectric transmission circuits where sudden interruption of load currents may cause overshoot of recovery voltages.

These new high-interrupting-capacity power fuses can now be applied to important high-voltage transmission systems to rapidly clear faults in connected equipment, thereby enhancing the reliability of the complete system. They also provide an economical means for tapping off small blocks of power from high-capacity systems.

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three general groupings, one for the subcenter, one for the central office, and one for the patron offices.

Subcenter

The subcenter switching equipment consists primarily of electrically operated multicontact relays and multicontact switches mounted either on racks or in cabinets, and in their assembled form termed "subcenter switching units." The relays are used primarily for control and signaling purposes, and the switches for making connections between the trunks and the patron tie-lines.

The switches are of the well-known Strowger type of two-motion and rotary switches. The two-motion switch is used to select the desired patron's line wire when a call is originated at the central office, and the rotary switch is used to seek the patron line wire when a call is made from the patron's office. The corresponding contacts of the contact banks which form a part of the two-motion and rotary switches are connected in multiple so that all of the patron tie-lines are available to all of the switches. The wipers of each switch are associated with a group of equipment individual to one trunk and complete the connections between that equipment and the equipment associated with the patron's line.

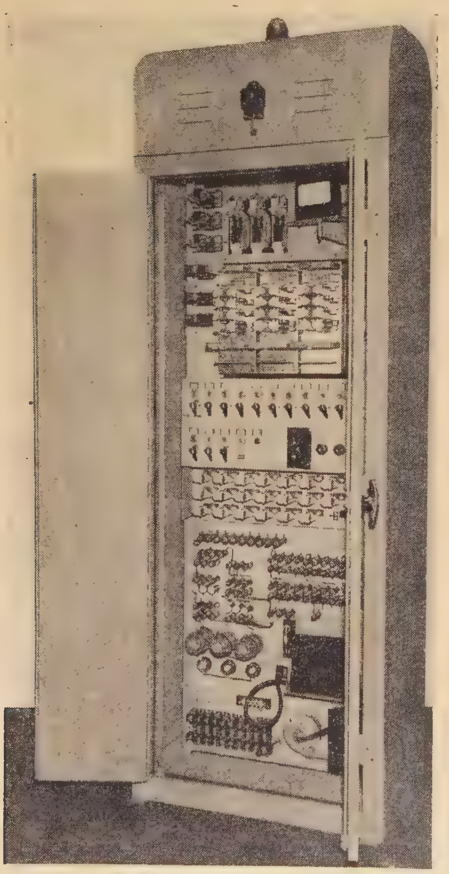

Figure 1. Small-size subcenter switching unit for ten patron tie lines and three central-office trunks

Three types of trunks, termed answering, calling, and combination trunks, are used for connecting the subcenter to the central office. The "answering trunks" are the trunks over which connections are made only when the calls are originated at patron offices; the "calling trunks," only when the calls are made at the central office; and the "combination trunks," when the calls are made at either the patron offices or the central office. The combination trunks, with the exception of the smallest size installations, require as much equipment in the subcenter switching units as an answering and a calling trunk combined, and, therefore, but few of them are used in any one installation. When installed, they are included primarily as overflow trunks, since they function as either answering or calling trunks.

Central Office

The central office equipment may consist either of a group of operating tables, termed "subcenter operating tables," with each table connected directly to a trunk, or of existing teleprinter concentrator equipment.

The subcenter operating tables are classified as "answering," "calling," or "combination," the classification depending on the type of trunk with which they are to be used. The equipment required is basically the same for all of them. In general, it includes a teleprinter, a signal lamp, control relays, and in the case of a calling or combination trunk, a dialing unit. The teleprinters, signal lamps, and dialing units are mounted on the table tops and the control relays in a metal box underneath.

The subcenters also are designed to direct the calls of all patron offices to the central office over as few trunks as possible by always switching the patron lines to the lowest numbered idle trunk. That feature enables them also to perform the functions of an automatic concentrator for the central office. Although they function as concentrators themselves their central-office trunks are often connected to concentrators at the central office for converging the business of one subcenter with that of another, and also with that of the patron tie-lines connected directly to the concentrator.

The equipment of teleprinter concentrators consists, essentially, of turrets, control relays, signaling lamps, and operating tables. Each turret contains a large number of jacks, and one is located convenient to each operating table. Each turret jack is multipled to the corresponding jack of all of the other turrets, and each of the multiple connections is connected to a separate incoming line which may be either a teleprinter tie-line or an answering trunk from a subcenter. Thus, all of the directly connected patron tie-lines and the answering trunks from the subcenter are connected to all of the turrets so that any line or trunk can be worked from any turret.

Each operating table of the concentrator contains, primarily, a teleprinter and a circuit from the teleprinter through control relays to a cord and plug located at a turret. As many operating tables are provided as are likely to be required at any one time. Connection is made between an operating table and a desired patron's line or answering trunk from a subcenter by inserting the plug of that operating table in the proper jack of the turret.

Each of the calling trunks from a subcenter is connected directly to one of the operating tables of the concentrator and can be worked only from that operating table. A dialing unit containing a dial, similar in general appearance to the dials used in automatic telephone systems, and a control key switch, is added to the equipment and circuits which normally form a part of those operating tables. The key switch is used for adapting the operating table to work with either its directly connected calling trunk from a subcenter or with any line connected to the turret jacks, and the dialing unit is used to select the line of any of the patron offices connected to the subcenter by controlling the selecting mechanism of that equipment.

Each combination trunk from a subcenter is connected both to a multipled jack circuit in the turrets and to one of the operating tables, since it functions as either an answering or calling trunk.

When the combination trunk is in use as an answering trunk, it is automatically disconnected from the operating table to which it is normally connected, and, when it is used as a calling trunk, it is automatically disconnected from the turrets. Also, when it is used as a calling trunk, the equipment associated with it at the subcenter automatically busies it to all patron calls. Hence, safeguards are included to prevent interference to service from any attempt to use this circuit for its two functions at the same time.

Patron's Office

The equipment at the patron's office consists, primarily, of a teleprinter, a signal lamp, a polar relay, and a teleprinter

operating table. That is the same teleprinter tie-line equipment which is used for operation with concentrators. The signal lamp is controlled by the polar relay and serves to advise the operator when the central office calls.

Standard Systems

Each of the first subcenter switching systems was devised for a specific installation, but the later ones were designed as standard systems for general usage. The latter differ from the first in flexibility, in the ultimate sizes of the installations, and in some details of circuit design. They were designed for three different sizes of installations, small, intermediate, and large, so that an economically feasible one would be available for any probable requirement.

Small System

The smallest of the systems enables groups of ten patrons to be served by a remote central office over three trunks. The subcenter switching equipment is housed in a single cabinet as illustrated in Figure 1.

Combination trunks, only, are provided between the central office and the subcenter, and the same equipment at the subcenter is used for calls originating at either the central or the patron's office. This differs from the intermediate and large systems, described later, in that those systems require two sets of equipment at the subcenter, one calling and one answering, for each combination trunk.

Provision is made in the design of the units for connecting two of them together in a single installation to provide for 20 patrons and six trunks when the requirements grow beyond the capacity of one unit.

Intermediate System

The next, or intermediate, size system is designed to serve groups of 25 patrons over eight trunks to the central office. The equipment of the subcenter, like that of the small system, is housed in a single cabinet (Figure 2).

Normally, three calling and five answering trunks are provided between the subcenter and the central office. Combination trunks may be provided by connecting together, at the subcenter, the equipment of one calling and of one answering trunk for each combination trunk desired. Hence, each combination trunk provided reduces by one the total number of central-office trunks that can be accommodated by the subcenter unit. A maximum of three combination trunks may be provided in this manner for each unit.

Provision also is made in the design of the subcenter switching units of this system for connecting two of them together in a single installation to double the capacity to 50 patrons, six calling and ten answering trunks.

Large System

Each of the small and intermediate systems requires that all of the patrons be connected to the one subcenter in order to share the trunks to the central office. The large system, in addition to serving a much larger number of patrons, also provides for serving the trunks of as many as seven smaller size subcenters. Thus, the large system may consist of one main subcenter serving 200 patron tie-lines and as many as seven tributary subcenters, each of which may serve 25 patron tie-lines, combined into a single large system serving a total of 375 patrons. The main subcenter, to which the central-office trunks are terminated, is termed the "subcenter," while the seven tributary subcenters are termed "sub-subcenters." The sub-subcenters may be either the 10 or the 25 patron size already described. Combining a large number of patron tie-lines in one system in that manner considerably reduces the number of trunks that would be required between the locality of the patrons and the distant central office if those patron tie-lines were served by several smaller systems. The plan of this system is illustrated by the diagram of Figure 3.

The equipment of the subcenter is assembled in nine units (see Figure 3). Two of the units contain that portion associated directly with the calling trunks; two, that portion associated directly with the answering trunks; one, that portion associated directly with the sub-subcenter answering trunks; and, four, that portion associated with the directly connected patrons. Each calling trunk unit provides for eight calling trunks to the central office and ten sub-subcenter calling trunks; each answering trunk unit, for 16 answering trunks to the central office; each patron tie-line unit, for 50 patron tie-lines; and, the sub-subcenter answering trunk unit for 35 sub-subcenter answering trunks. Hence, an installation equipped for maximum capacity operation provides for 16 central-office calling trunks, 32 central-office answering trunks, 200 directly connected patron tie-lines, and 55 sub-subcenter trunks.

All of the patrons, regardless of whether they are connected to the subcenter directly or by sub-subcenter trunks, share the use of all of the central-office trunks.

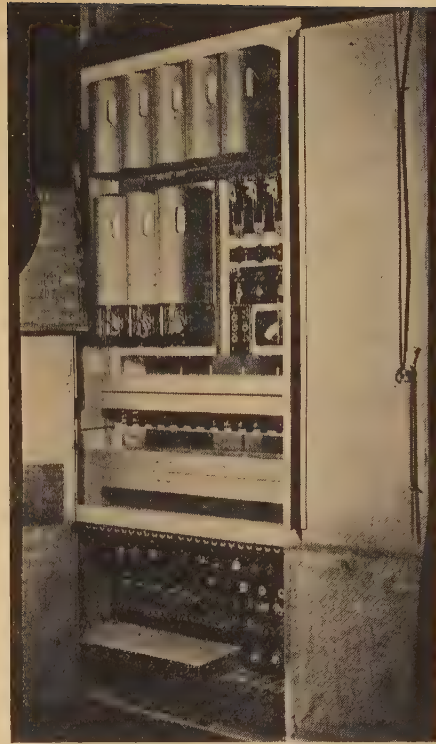

Figure 2. Intermediate-size subcenter switching unit for 25 patron tie lines and eight central-office trunks

The trunks between the subcenter and the central office also may be converted to combination trunks by combining the equipment in the subcenter of one calling and of one answering trunk for each combination trunk desired. A maximum of 16 combination trunks can be obtained in that manner.

Operation

All of the subcenter switching systems mentioned in this paper operate fundamentally in the same manner.

Calls can be originated from either the central or any patron's office, but connections can be completed only between the central and a patron's office. No provision is made for making connections between any two patron offices.

After a connection is made, transmission may be carried on in either direction until the connection is released, regardless of which office initiated the call. The connections normally are released only from the central office.

The operations required at either the central or the patrons' offices for causing the subcenter switching equipment to effect a connection are so simple that no special training of the personnel is necessary for proper operation.

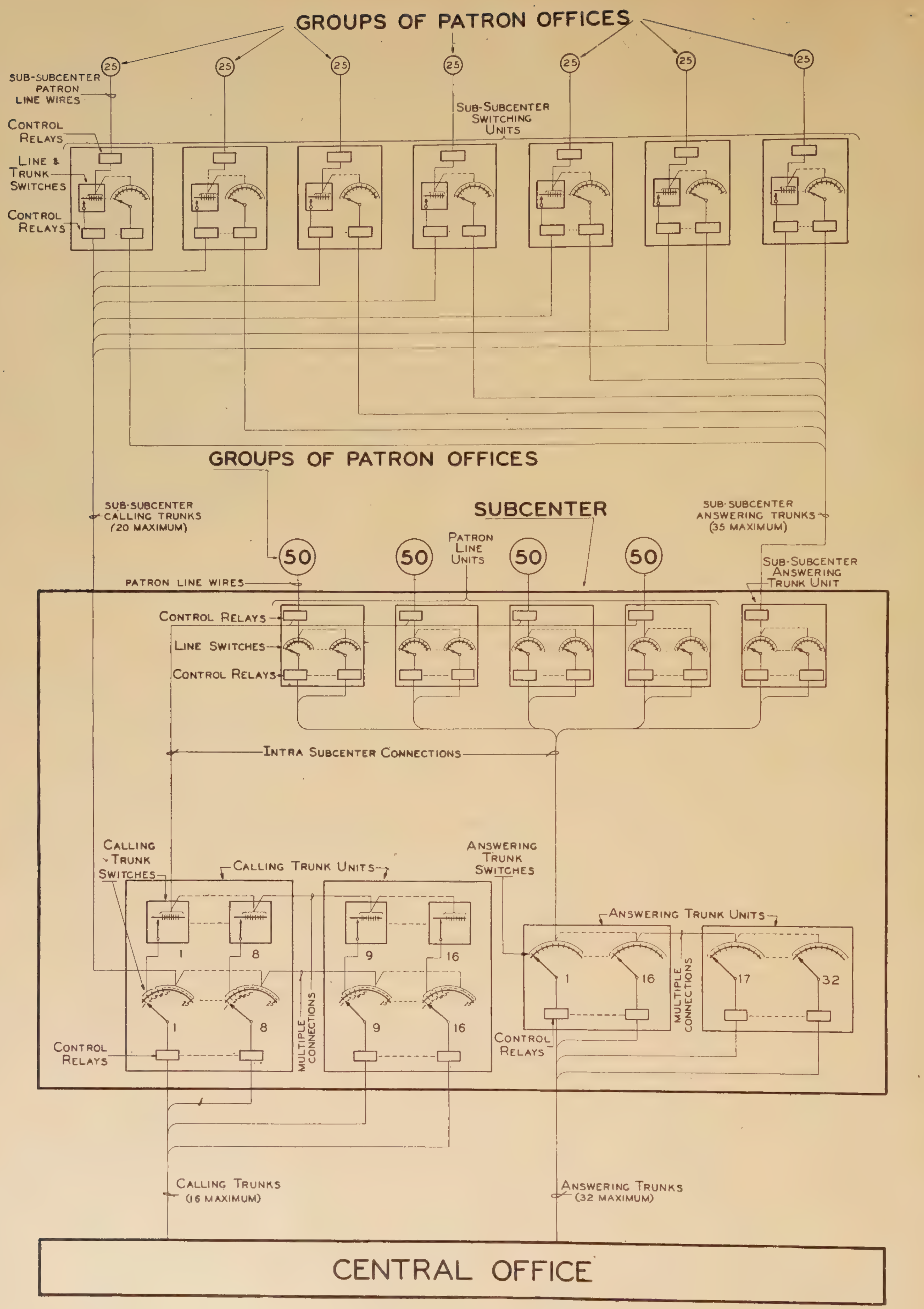

Figure 3. Diagram showing plan of large subcenter switching system

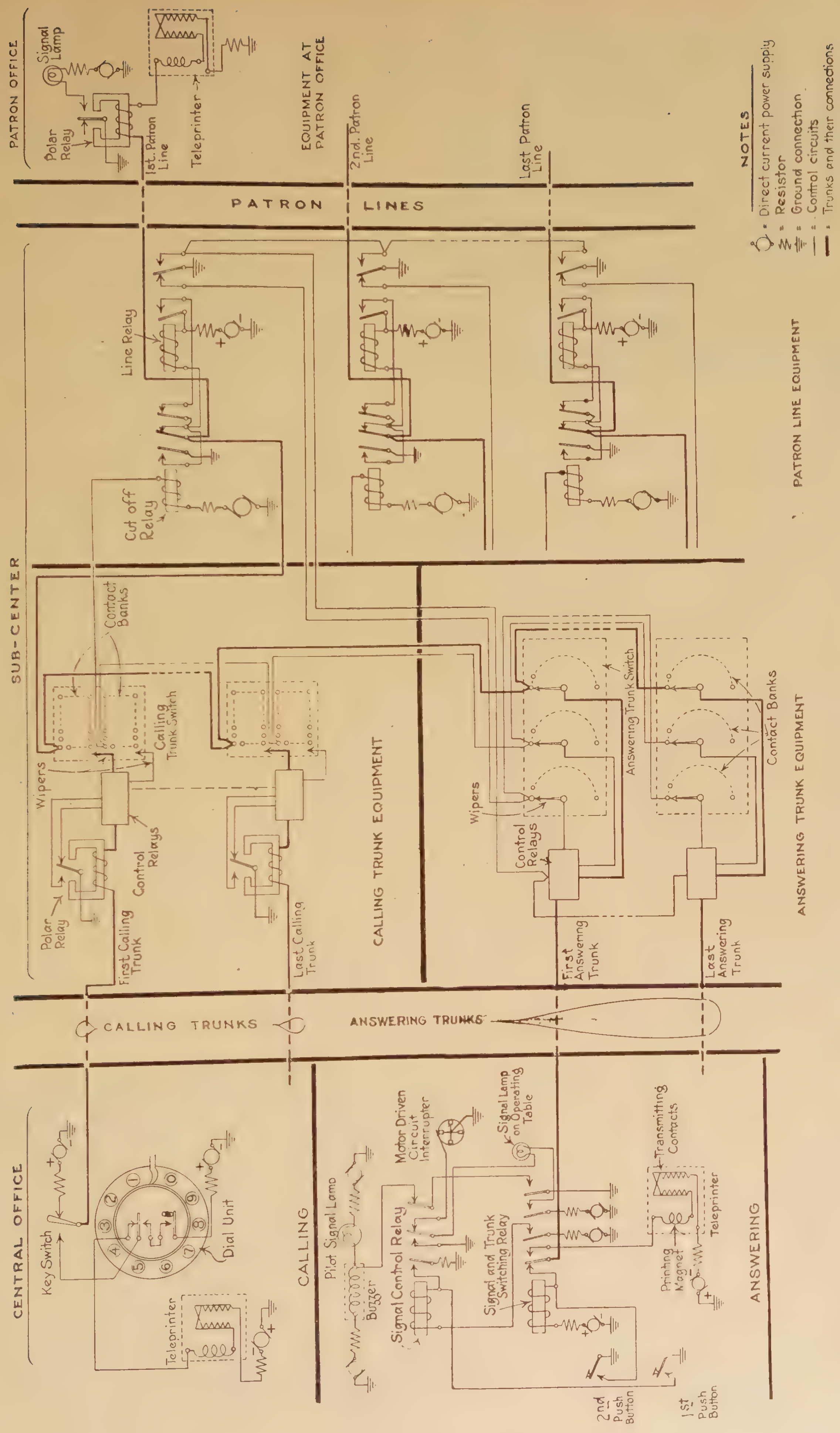

Figure 4. Simplified schematic diagram of the circuit and equipment used in a typical subcenter switching system

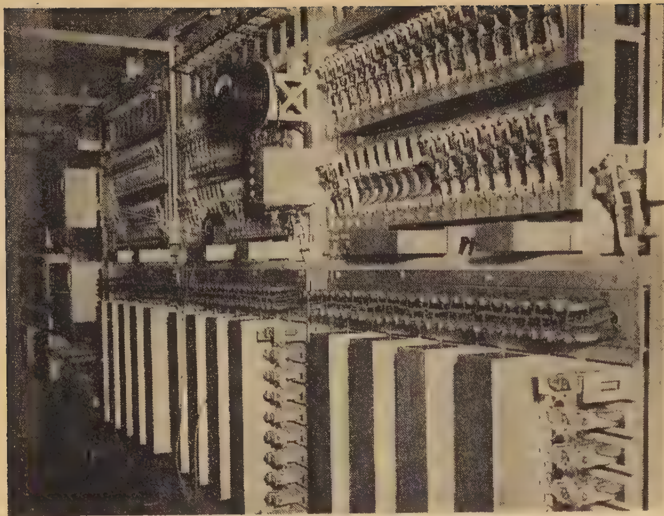

Figure 5. View of switching equipment at Newark for large-size subcenter-switching system

Figure 4 is a simplified schematic diagram of the circuit and equipment arrangements used in a typical subcenter switching system. The trunks are shown terminated on individual tables in the central office. Many details of the control circuits and relays are omitted from the diagram for reasons of simplicity.

A call originated at any patron's office is initiated by the patron's operator opening, momentarily, the normally closed line between her office and the subcenter by depressing any one of the keys of her teleprinter keyboard. The opening of the line allows a normally operated relay, termed "line relay," connected in series with the line in the subcenter switching unit, to release and lock itself released.

The release of that relay causes the wiper contacts of the switch associated with the lowest numbered idle answering trunk, through the medium of control circuits and relays, to seek the patrons' line containing that relay by advancing, step by step, over the contacts of its contact bank.

When that patron's connections are found, control circuits and relays associated with the respective switch and the patron's line function to disconnect ground from the trunk, and power from the patron's line, and after a brief lapse of time (about 0.3 second), to connect the trunk and the patron's line together. The brief interval of time between the removal of ground from the trunk and the connecting of the trunk to the patron's line wire constitutes a sufficient opening of the trunk to allow a normally operated relay whose winding is connected in series with the trunk at the central office to release and operate the central office signaling equipment.

When the answering trunks are connected directly to individual tables at the central office, the release of the relay, termed a "signaling and line switching relay," not only signals the operator but also connects the central-office teleprinter to the trunk and reverses the polarity of the power applied to the trunk from positive to negative.

Both visual and audible signals are provided at the central office. A pilot lamp and buzzer, conspicuously located, give a continuous alarm, and a signal lamp, mounted on the table to which the call is directed, flashes repeatedly until an operator presses a push button on the table top for operating the signal control relay. Those signals advise the operators that a new call has been received, and the push button is pressed when the call is answered. The operation of the signal control relay silences the buzzer, extinguishes the pilot lamp, and causes the signal lamp on the table to glow steadily, instead of flashing, and to continue glowing as long as the connection is allowed to remain. This alarm signaling arrangement is used to insure that all calls are given prompt attention, and the continuous glowing of the signal lamp is provided for supervisory purposes.

The reversal of the power applied to the trunk, from positive to negative, causes a polar relay, connected in series with the trunk at the subcenter, to reverse its armature and prepare the subcenter switching equipment for releasing the connection when the transmission of business is completed.

When the transmission is completed, the central-office operator presses a second push button, which reoperates the signaling and line switching relay. The operation of that relay transfers the trunk from the teleprinter and power of negative polarity to a circuit through its own winding and power of positive polarity. It also opens the circuits to the signal lamp and the signal control relay winding causing the lamp to be extinguished and the relay to release. The reversal of power to the trunk from negative to positive causes the armature of the polar relay, whose winding is connected in series with the trunk in the subcenter, to reverse to its original position and release the connection. The trunk is then available for connection to another patron's line wire.

When the answering trunks are terminated at a teleprinter concentrator, the opening of the trunk at the subcenter causes only a signal to be given at the central office for identifying the trunk and attracting the attention of the operator. The reversal of power polarity to the subcenter, from positive to negative, is effected by the operator's inserting the plug of her operating table circuit into the proper jack of the turret. This operation transfers the trunk from its normal idle connection and positive polarity of power to the operating table circuit and negative power.

When transmission is completed, the plug is removed from the jack, causing the power applied to the trunk to be changed to positive, which is the idle condition

Figure 6. Section of the Western Union central office in New York. This view shows some of the subcenter operating tables used with the Newark subcenter switching system

polarity. Hence, the plug and jack, with their associated relays, perform the same functions at the central office when concentrators are used as the signaling and trunk switching relay and the push buttons when individual tables are used.

Calls are initiated at the central office by reversing the power applied to the trunk from positive to negative by operating a key switch and by dialing the numeral assigned to the desired patron office in the same manner as the digits of dial telephone numbers are dialed. The reversal of power polarity prepares the subcenter equipment, associated with the calling trunk, for responding to the dial pulses and for advancing the wiper contacts of the switch to the contacts of its bank associated with the desired patron's line.

When the proper contacts are reached, the patron's line is first tested automatically at the subcenter to determine whether it is idle or is busy. If it is busy, an automatic busy-signal transmitting device sends a busy signal over the calling trunk to the central-office operator. If it is idle, control relays at the subcenter associated with the trunk and the dialed patron's line function to disconnect the patron's line from positive power at the subcenter and connect it to the trunk line which, at that time, has power of negative polarity applied at the central office. This reverses the direction of current over the patron's line and causes a polar relay connected in series with the line at the patron's office to operate and signal the call. The patron's operator answers the call with her teleprinter keyboard.

When transmission is completed, the central-office operator restores the lever of her key switch to its normal idle position which restores the central-office circuits and equipment to their idle condition, and reverses the polarity of the power applied to the trunk for releasing the connection at the subcenter. The trunk then is available for connection to another patron's line.

Several interesting auxiliary features are contained in the switching units. One of them is a means for automatically wiping out false calls on patron's lines which might result in the event of a temporary interruption of the local power supply to the subcenter switching units. Since power is furnished the patron line wires from the subcenter switching units, any interruption of that power results in an interruption of the current flow over the line wires. Any interruption of the current flow over the patron's line wires allows the line relays of all the patron lines at the subcenter to release in the same manner as they do for bona fide patron calls. If automatic preventive measures were not included in the design, each patron line wire would be switched to the central office as fast as answering trunks became available until all had been connected to and released by the central office. The false call wipe-out feature functions after each power failure to prevent all switches associated with answering trunks from operating, to re-establish the patron line-wire relays at the subcenter to their normal idle operated condition, and then to allow all switches to operate normally again.

Another interesting feature is a provision for "busying" the equipment at the subcenter of any answering trunk to all new calls by opening the answering trunk circuit. That enables the central-office attendants to remove any trunk from service for maintenance or for any other reason, and it also automatically prevents the connecting of a patron's line to an open trunk.

Installations

Examples of the benefits which subcenter switching systems can render are afforded by the installations which have been in service for several years between patrons in Oakland and the central office in San Francisco, patrons in Beverly Hills and the central office in Los Angeles, and patrons in Newark and the central office in New York City.

The Oakland–San Francisco system was designed to enable the central office in San Francisco to serve 50 patrons located in Oakland over 16 submarine cable conductors (six calling and ten answering trunks) across San Francisco Bay. All trunks were terminated in an existing teleprinter concentrator at the central office in San Francisco.

The Beverly Hills–Los Angeles system utilizes one of the 25 patron subcenter switching units previously described and shown in Figure 2. All trunks are terminated in an existing teleprinter concentrator at the central office in Los Angeles. This installation relieved an acute wire shortage caused by furnishing direct teleprinter tie-line service between those points to 15 patrons, and at the same time provided accommodations for extending the service to ten more such patrons.

The Newark–New York City system was the first of the larger systems designed and installed. Though the installation as now equipped accommodates only 150 patrons, the design provides for adding equipment to increase this number to 200 whenever the need for it develops.

The equipment of the subcenter, which is located in Newark, is housed in five racks as shown in Figure 5, and these racks are enclosed in an air-conditioned room for protecting the equipment from dust. The three similar racks shown in the foreground are the patron line units, the fourth is the calling trunk unit, and the fifth is the answering trunk unit. One patron line unit has a capacity of 50 patron tie-lines; one calling trunk unit, ten calling trunks; and one answering trunk unit, 36 answering trunks.

The system, at present, is served by 27 trunks, two of which are calling, five are combination, and 20 are answering. These trunks are terminated on individual tables in the central office in New York. Figure 6 is a view of that portion of the central office in New York containing most of those tables.

The first two installations, the Oakland–San Francisco and the Beverly Hills–Los Angeles, effected substantial savings in line wires and increased the capacity of the Western Union to furnish more of the same type of service between those points.

The Newark–New York installation greatly reduced the cost of operation by eliminating one relaying, or retransmission, of the patron business at Newark.

In the past, the business was received in the Newark central office and most of it was retransmitted to one of the few large central offices connected to it, for further retransmission to its destination.

At present that part of the business destined for New York is handled directly, and the rest is retransmitted either to its destination or to a point much closer than was possible before, since the New York office is provided with direct connections to many more cities than was the Newark office.

The benefits anticipated from the use of subcenter switching systems have been fully realized in the installations made thus far.

Ease of operation, improved speed of service, saving of line wires, and in some instances, such as the Newark case, the reduction of operating costs, are the factors which make this development promising for future use in the telegraph industry.

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Aircraft Circuit Breakers

C. W. KUHN
FELLOW AIEE

Synopsis: Airplane circuit breakers are recognized as important units in the electrical systems of military aircraft. The design of such circuit breakers can be combined with switching and contactor functions. This makes possible the use of well-developed switch and contactor structures.

The design of such circuit breakers must provide for operation under the very exacting requirements of military aircraft service.

The principal function of both manual and remote-control circuit breakers is to provide short-circuit protection. There is some question as to the desirability of motor protection for military aircraft. Many motor functions are vital to the operation of the aircraft, and it may be better to operate the motor to destruction rather than to stop at a safe temperature or load value. Where motor overload protection is desired, it is necessary to correlate circuit-breaker and motor characteristics. Both manual and remote-control circuit breakers can be made to provide for holding the circuit closed in emergency to "force" the motor or other equipment.

ELECTRICAL operation and control have contributed largely to the effectiveness of today's military aircraft. It has been necessary to develop new types of electrical equipment to meet the special requirements of this service. Minimum size and weight are of extreme importance.[1] Control devices must operate in any position, must withstand high rates of acceleration and retardation, must operate in ambient temperatures of −50 to 200 degrees Fahrenheit, must operate at high altitude with rarefied atmosphere, and must withstand severe vibration.

Extensive electrical power systems are required for these military aircraft, particularly for the large bombers. Maximum reliability for the various circuits for these power systems is of unusual importance. Any part of the electrical power systems may be damaged in battle. As much of it as possible should remain available. It should be possible to restore power to any circuit as soon as a fault is cleared. Airplane-type circuit breakers are the logical answer to these problems.

Twenty-four volt d-c power with stand-by storage battery is the most extensively used power system for United States military aircraft. The lower voltage as compared to industrial power reduces the interrupting problem for circuit breakers but increases current values for a given power rating. Short-circuit faults in such a system may result in current of from 1,000 to 3,000 amperes. Such service presents serious contact problems.

These contact problems have been largely solved in the development of aircraft manual switches and contactors. One solution to the aircraft-circuit-breaker problem, therefore, is the addition of thermal trip units to these aircraft switches and contactor. Such an arrangement has a number of obvious advantages. The manual switches and contactors can be used for their normal service. The same mounting arrangements are available. Repairs and replacements are simplified. This type of design is a dual purpose one in which circuit or overload protection has been added to the normal switching or contactor function. Our discussion here will be limited to the features of circuit or overload protection.

The thermal trip units must be designed with special consideration for the service requirements of military aircraft. Their tripping values must not be appreciably affected by acceleration of 10*g* (where *g* is the acceleration of gravity) or more, or the effect of severe vibration. They must not change in rating in altitude from sea level to approximately 40,000 feet. These results can be accomplished by proper design.

Manual Circuit Breakers

The manual-type circuit breaker consisting of an airplane-type switch with bimetal thermal trip provides both circuit protection and switching functions. The X-ray pictures, Figures 1, 2, and 3, show the general construction of one such airplane circuit breaker. The switch mechanism provides make and break contacts operated by a toggle lever. This toggle lever may be provided with a luminous or fluorescent end. The trip unit is of the bimetal type. The bimetal latches a spring-actuated trip member. When the current through this bimetal trip unit exceeds a predetermined value for a sufficient length of time, the bimetal trips the latch and causes the circuit breaker to open its contacts. The contacts are reset by returning the toggle lever to the "on" position.

A thermal trip unit, in which the current flows through the bimetal, has a relatively sharp knee in the tripping-time curve. This is favorable to the protection of the circuit, as the wiring cable likewise has but little thermal capacity and reaches its maximum permissible temperature in a relatively short time. The relationship between the time re-

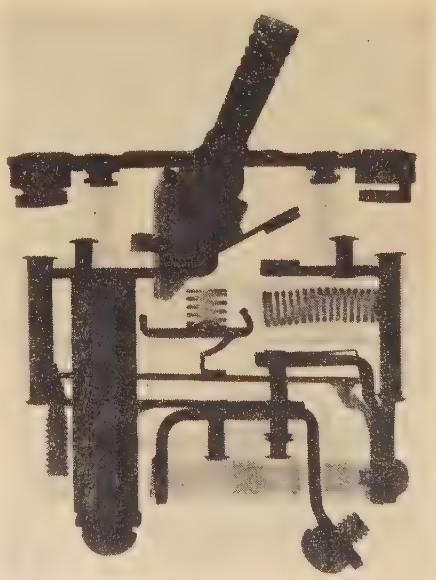

Figure 1. Manual circuit breaker
Contacts open, thermal trip unit latched

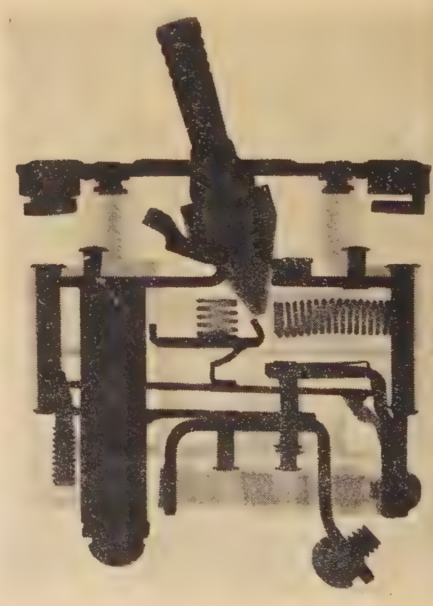

Figure 2. Manual circuit breaker
Contacts closed, thermal trip unit latched

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C. W. KUHN is supervising engineer, development department, Cutler-Hammer Inc., Milwaukee, Wis.

quired for the cable to reach a temperature of 90 degrees centigrade as compared to the tripping time of a circuit breaker with a tripping temperature of 150 degrees centigrade, is shown in Figure 4.

Short-circuit protection is the principal function of the manual circuit breaker. The amount of short-circuit current is determined by the circuit. High-capacity circuits will generally have higher short-circuit currents than low-capacity circuits. This is illustrated by the accompanying table which shows standard manual-circuit-breaker ratings and short-circuit test currents.

Table II of Army Navy Aeronautical Standard *AN-C-77, February, 1943*

Breaker Rating (Amperes)	Short-Circuit Load (Amperes)
5	1,000
10	1,000
15	1,500
20	1,500
25	2,000
35	2,500
50	3,000

The service requirement for aircraft circuit breakers differs in other particulars from those used in industry. Some of the applications make it desirable to "force" operation of the equipment even though the motors may seriously overheat. Specifications, therefore, require the nontrip-free type of circuit breaker. This is based on tripping of the circuit breaker to protect the circuit and equipment in case of faults, yet permitting the operator to "force" the motors, in case of emergency, by holding the circuit breaker in the closed position.

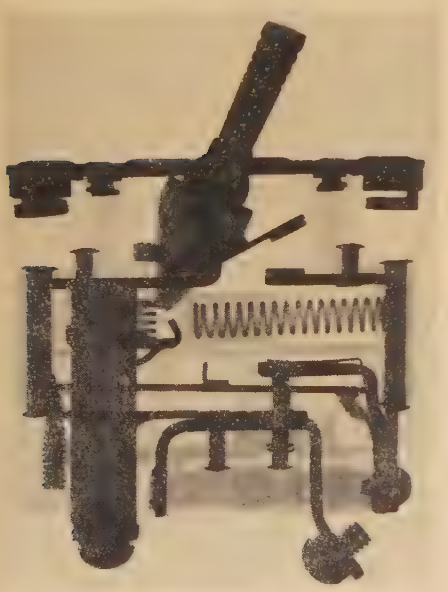

Figure 3. Thermal trip unit tripped, contacts open

Remote-Control Circuit Breakers

There are some applications, particularly on the larger military aircraft, where it is desirable to have the circuit breaker at a location remote from the point of control. Such installation reduces the amount of heavy wiring, thereby reducing the important element of weight. The reduced amount of heavy wiring further tends to increase reliability, because of damage from enemy fire.

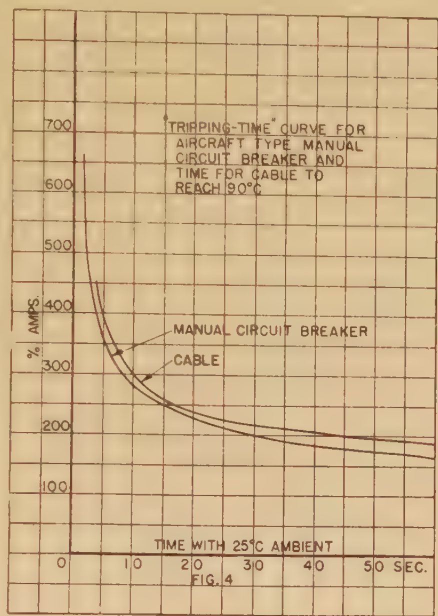

Figure 4

Circuit protection is the primary function of the remote-control circuit breaker. It is also possible to obtain motor protection, that is, to add the overload function to that of contactor and circuit breaker, making a triple-purpose device.

There is some question as to the desirability of motor protection for military aircraft. Many motor functions are vital to the operation of the aircraft, and it may be better to operate the motor to destruction rather than to stop at a safe temperature or load value. The following discussion, however, pertains to those applications where motor protection may be desired.

A remote-control circuit breaker consisting of an airplane-type contactor with thermal trip unit is shown in Figure 5. This combination can be obtained in the various standard sizes of contactor as used for airplane service, that is, 50-ampere, 100-ampere, and 200-ampere ratings. These airplane contactors provide the high interrupting capacity (3,000 amperes) and freedom from welding of contacts as required for circuit-breaker service.

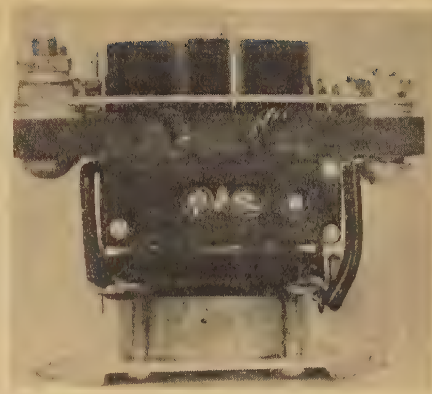

Figure 5. Remote-control circuit breaker

The thermal trip unit for these remote-control circuit breakers is shown in the X-ray photographs of Figures 6 and 7. This provides quick make and break pilot-duty contacts operated by a bimetal trip member. The contacts are insulated from the bimetal trip member by a small bakelite operating pin. The bimetal carries all or part of the main circuit current. The different ratings are obtained by the use of shunt elements which are an integral welded unit with the bimetal.

It is well established that the tripping temperature of an overload device should approximate the danger temperature of the equipment to be protected. For military aircraft it is desirable to operate to maximum capacity to avoid unnecessary weight. Class *A* insulation will permit of operation at temperatures of 125 degrees centigrade. This is recognized in the Underwriters' Laboratories standard on industrial control for built-in cycling overload motor protection.

The heating of any motor is, in general, proportional to the losses in the motor. It is, therefore, possible to determine an approximate heating curve for a motor if

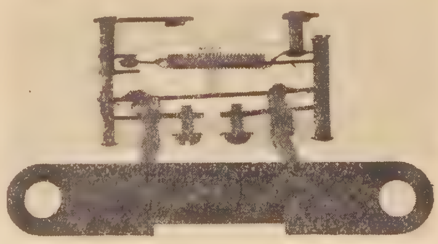

Figure 6. Remote-control circuit-breaker thermal trip unit

Contacts closed

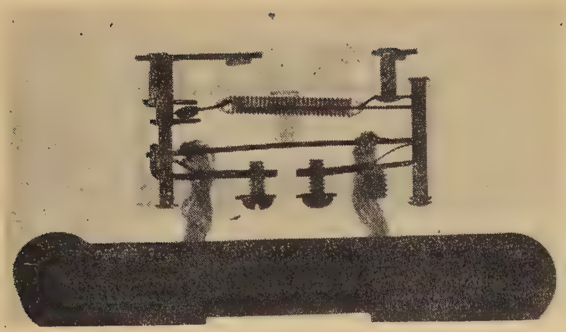

Figure 7. Remote-control circuit-breaker thermal trip unit

Contacts open

we have the efficiency curve and know the temperature rise at some particular loading. Such a study based on performance curves for a two-horsepower aircraft-type motor is shown in Figure 8.

From the heating curve for the motor we can readily determine a load curve for a range of ambient temperatures. This is based on the assumption that the final temperature of the motor will equal the sum of the ambient temperature and temperature rise for a given load. The proper trip temperature, for the thermal trip unit, is readily determined from this curve. Figure 9 shows motor load and thermal unit tripping-current curves. The motor load curve is based on a maximum winding temperature of 125 degrees centigrade and the curve for the thermal trip unit is based on trip temperature of 120 degrees centigrade.

The use of glass-insulated motor windings will of course permit of higher motor temperatures. Such motors require higher tripping temperature for overload protection. This is reflected to some extent in *AN-C-77* specifications which indicate an approximate tripping temperature of 155 degrees centigrade.

For motor protection it is necessary to provide a large number of ratings. These ratings should be spaced not more than ten per cent. If we protect the motor for a predetermined maximum temperature, it will be necessary to overprotect an amount equal to the spacing between ratings. This results in a corresponding loss in motor capacity.

Many aircraft motor applications are of the intermittent-duty type. The greatest need for motor protection on such application is protection against locked rotor. In order to determine such motor protection it is necessary to have the corresponding motor data. These include locked rotor current and the maximum time which the motor will permit of this current without exceeding the maximum permissible temperature. This maximum temperature may occur in either the field or armature windings. Many of these intermittent-duty applications are such that if the motor will operate at all, no further protection is needed. For such applications the remote-control circuit breaker provides motor protection equal to that which could be obtained with built-in overload protection. The remote-control circuit breaker provides quick reset for such applications, whereas built-in overload protection depends upon cooling of the motor and has a much longer reset time.

Circuit-breaker standards specify ratings which will cause tripping at 25 degrees centigrade from 115 per cent to 138 per cent of the established rating. This is primarily intended for circuit protection and to insure that rated load will not cause tripping. This method of rating must be understood when making applications of remote-control circuit breakers for motor protection. These ratings, however, can be readily translated into the usual overload ratings which provide that the overload unit trip under all conditions in a 40 degrees centigrade ambient. The recognized standard of accuracy for overload protection is a tolerance of +0 to −10 per cent.

Remote-control circuit breakers for many military aircraft applications should be arranged for "forcing" the motor in emergencies. It is also desirable to indicate tripping of a remote-control circuit breaker at the control point. These functions can be obtained by control circuits as shown on Figure 10. This circuit requires but a single control wire with grounded return. The use of the two-way momentary contact pilot switch provides six operating functions.

1. The contactor is closed by moving the pilot switch to the close position and is maintained through a control resistance which is mounted as a part of the remote-control circuit breaker.

2. The contacts are opened by moving the pilot switch to the open position.

3. The thermal trip unit contacts reclose automatically after tripping on overload, and operation to the close position re-establishes circuit to the load.

4. The motor can be forced against overload by holding the two-way momentary contact switch in close position.

5. The remote-control magnetic indicator is operated through the single control wire to show the position of the contactor.

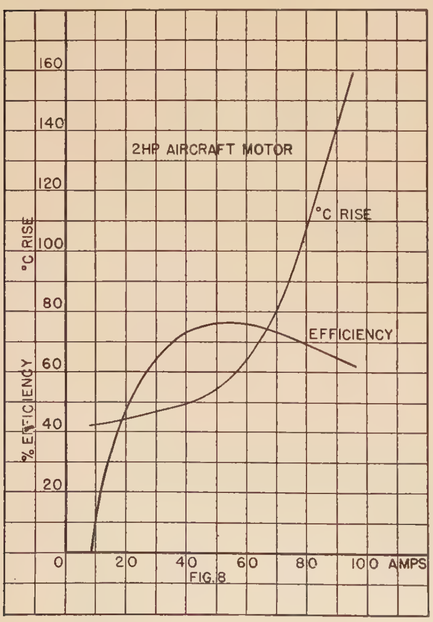

Figure 8

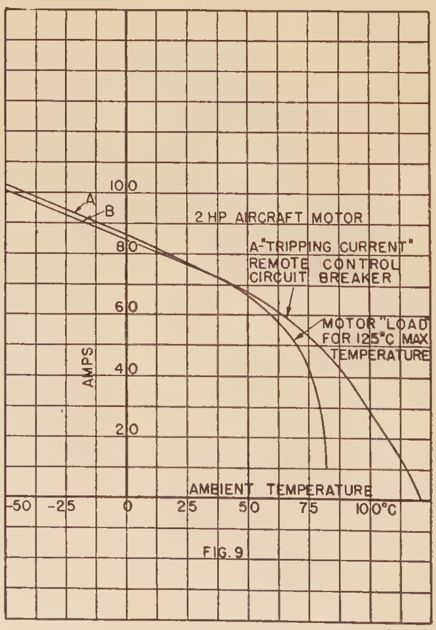

Figure 9

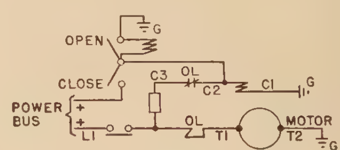

Figure 10

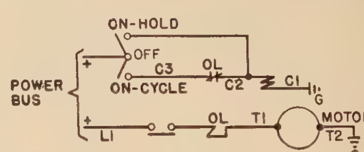

Figure 11

Relay Protection of Tapped Transmission Lines

M. A. BOSTWICK
ASSOCIATE AIEE

E. L. HARDER
MEMBER AIEE

Synopsis: The war, with its attendant shortage of critical materials, has led to a considerable increase in the number of tapped lines. This paper deals with the method of protecting circuits of this kind under the three headings:

1. Pilot-wire protection, a-c and d-c schemes.
2. Carrier-current protection.
3. Other protective schemes.

Directional overcurrent or network.
Distance type.

Three-terminal lines are classified into three types with regard to other system connections between the terminals connected by the line in question. These are further classified as to power sources to aid in analyzing limiting conditions for relay applications.

Typical operating characteristics of a-c pilot-wire equipment for three-terminal lines are shown.

In general in dealing with pilot-wire, carrier, and other types of protection, those problems peculiar to application on three-terminal lines are analyzed and methods given for solving some of the more troublesome problems.

Introduction

THE interconnection of several power supply and load points by a single tapped transmission line rather than by a number of two-terminal lines has always presented first cost advantages at some expense in reliability and flexibility. However, the relay protection of such lines has been somewhat of a nightmare to protection engineers. Increased operating times for complete clearing of faults, and even some loss of selectivity have had to be tolerated in many instances. It has been said that there are no good three-terminal lines; some are simply worse than others. While inaccurate, this thought does express the general sentiment of protection engineers toward such layouts.

Nevertheless, the war, with its attendant shortage of critical materials, and manpower for construction, has led to a considerable increase in the number of tapped lines and placed increased load requirements on others already in service. Many war plants, because of their locations and high power requirements have been provided with supply with a minimum use of critical materials by tapping the nearest high-voltage line or lines. The circuit breakers usually have been located on the low-voltage side of the transformer.

The present paper deals with the methods of protection for such lines under the headings of:

1. Directional overcurrent protection.
2. Impedance or distance-type protection.
3. Pilot-wire and carrier-pilot protection

It is hoped that by summarizing the existing schemes and presenting data on certain new schemes, particularly on pilot-wire protection, that some help can be given in the solution of this currently important problem. It is also hoped that this paper will elicit discussion which will bring to light other schemes which various protection engineers have devised to solve the problems here discussed. Such discussions will be of particular benefit to the industry at the present time.

Typical Tapped Lines

Tapped lines appear in all the categories of main transmission, subtransmission, and distribution. Two hydrogenerating stations may be connected together and to a load and steam generation area over a high-voltage three-terminal line of the main transmission class. The substations of an a-c electrified railway[2] are fed from dual 132-kv lines with as many as ten substation transformers tapped from each line. The trolley system constitutes a 12-kv network. In the subtransmission class the four-kv network[1] ("high-voltage network") is a typical example of multitapped lines, the transformers which feed the network being tapped from lines such as 13.8 kv. Industrial plants will be found tapped at intermediate points of lines of practically all voltage classes. In many cases these plants have generating facilities in order to provide process steam. Hence, they must be relayed for line faults. Numerous other examples could, of course, be cited.

Classification of Three-Terminal Lines

The simplest three-terminal line is the straight radial arrangement with one

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M. A. Bostwick is protective-relay application engineer, meter engineering department, Westinghouse Electric and Manufacturing Company, Newark, N. J. E. L. Harder, also with Westinghouse Electric and Manufacturing Company, is central-station engineer in the industry engineering department, East Pittsburgh, Pa.

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6. The contactor will maintain its last operating position, should the control wire be broken or shot away.

A second control scheme, Figure 11, provides operation of the remote-control contactor by means of a three-position pilot switch. The contactor can be held closed against motor overload if the pilot switch is in the "on-hold" position. The overload trip unit contacts reclose automatically, and, if the pilot switch be in the "on-cycle" position, the motor will restart automatically, after tripping on overload. If the control wire is shot away or broken, the contactor will open or remain open as the case may be.

Conclusions

Military aircraft have extensive electrical systems. Circuit breakers are an essential part of such an electrical system. Their use insures that as much of this system as possible will remain available when the aircraft is subjected to enemy fire. These circuit breakers can combine switch and contactor functions with that of circuit protection, thereby reducing weight.

Military aircraft service often requires the immediate and maximum operation of motors so that motor overload protection cannot be provided. For those applications, where motor-overload protection is desired, circuit-breaker characteristics must be properly related to motor characteristics. Some such applications are of very intermittent duty and stalled motor conditions is the principle consideration for motor protection. Both manual and remote-control circuit breakers can be of the nontrip-free type to permit the operator to "force" the motor or other circuit devices beyond conditions which cause tripping.

Reference

1. Application of Electric Power in Aircraft, T. B. Holliday. Electrical Engineering, volume 60, May 1941, pages 218–25.

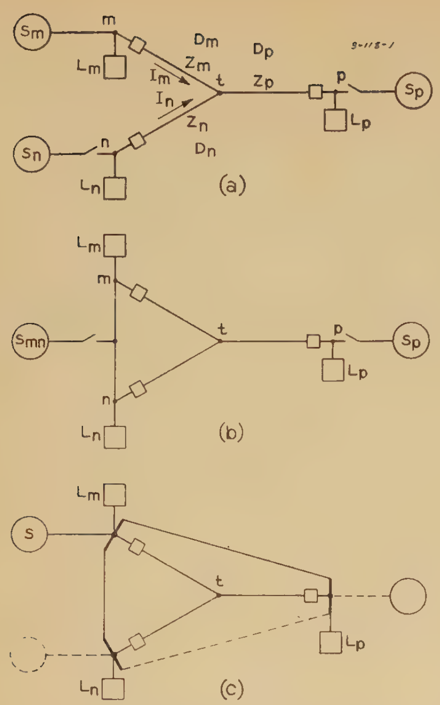


Figure 1. Classification of three-terminal lines

(a). No paralleling ties; one, two or three sources

(b). One paralleling tie; sources on connected terminal, or independent terminal, or both

(c). Paralleling ties between all terminals

S—Power source
L—Load
Z—Impedance

power source and two loads, fed solely over the line in question. As the number of sources is increased or other ties are included between the busses connected by the three-terminal line, additional relaying problems are introduced. Thus, from a relay-protection point of view, three-terminal lines may be classified into seven types as follows:

A. No paralleling ties, Figure 1a.
1. One power source.
2. Two power sources.
3. Three power sources.

B. Paralleling tie between two of the terminals, Figure 1b.
4. Power source on the connected terminals only.
5. Power source on the independent terminal only.
6. Power sources on both.

C. Paralleling ties between all three terminals, Figure 1c (with or without dotted portions).
7. The power source is effective at all terminals.

The classification as regards ground current is, in general, different from that for phase currents. For example, Figure 2 illustrates a three-terminal line with paralleling connections between all terminals for phase currents. However, ground faults on the tapped line cause no ground current in the other two lines. Thus, there are no paralleling connections for ground currents. Also, the two grounded transformers are the only sources of ground current for the tapped line. Thus, the relaying for ground faults is little, if any, different from a two-terminal line. This particular three-terminal line falls in the seventh classification for phase relaying but in the second class for ground relaying. High-voltage bussing throughout would throw it into the seventh classification for ground relaying also.

The Problems of Tapped Lines

The greater difficulty of relay co-ordination with tapped lines is due to a variety of problems that do not arise with the simpler two-terminal lines. For example, the impedance from one terminal to the two others may be different, introducing a problem in setting distance-type relays. Also, an impedance measurement at one terminal for fault near a

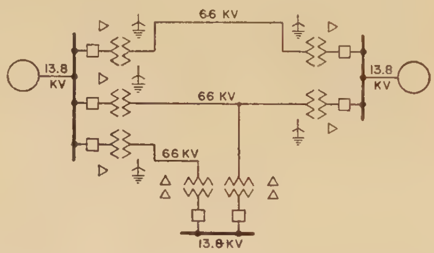


Figure 2. System showing three-terminal line with parallel ties for phase currents and only two sources with no parallel lines for ground currents

second is affected by current entering at the tap point from the third terminal, or by load current flowing to the third terminal. These two problems may occur on any of the seven classifications of three-terminal lines.

In addition, on the latter four classes, which involve paralleling ties, it is possible for fault power to flow out of one terminal for a fault on the line near another terminal. This frequently prevents the use of a straight blocking pilot scheme on lines of this class.

These problems and their solutions are dealt with in the subsequent paragraphs. Quite satisfactory solutions are possible by the use of pilot-wire[3] and carrier-pilot[4] relaying where these are feasible. Frequently the directional overcurrent or distance schemes depend for good operation on the strict maintenance of a normal system setup, or else sacrifice speed to use settings that will co-ordinate under adverse system conditions. The high-voltage network is an exception as will be explained.

Directional Overcurrent Protection

Three-terminal lines in radial circuits can be protected with standard overcurrent time-delay relays, as indicated in Figure 3. When additional sources of power are connected to the circuits, directional elements must be added to the relays. This circuit is indicated by the dotted construction on Figure 3. As the amount of power that must be transmitted over a given circuit is increased, the problem of maintaining system stability during faults becomes more critical and may impose limitations on the amount of time that is allowable for clearing faults. This usually eliminates the use of timed overcurrent relays, except in special cases where instantaneous overcurrent elements will provide the required speed of operation.

The high-voltage network[1] is an ideal example of a multiterminal-line arrangement that can be satisfactorily relayed Each of the many taps is tripped by power reversal to the supply line, the system being so arranged that this can occur only during a fault on the supply line. This requires that all lines feeding a given network emanate from the same bus so that for normal load conditions the supply-line voltages are nearly equal and little or no reverse power flows through any transformer bank.

Many modifications of this basic scheme have been used in which some power reversal can take place when tripping is not desired. If such a reversed flow is due to a fault beyond the next breaker, time is introduced[1] for selection. If it is due to load currents, fault detectors have been successfully used, and, if the reversal can be due to either faults or loads,[2] fault detectors have been used to prevent operation on load currents and timing to provide selectivity for proper fault operation.

Usually such systems are arranged for

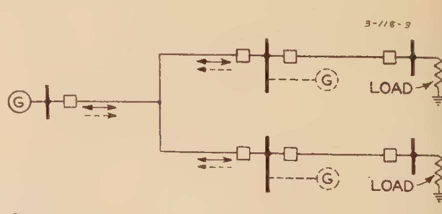


Figure 3. Overcurrent protection of three-terminal line

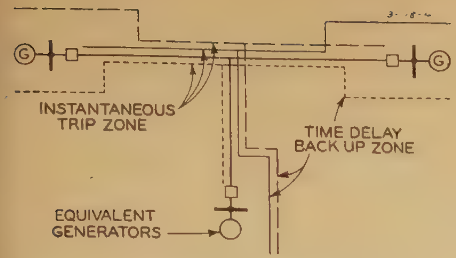


Figure 4. Impedance protection of three-terminal line

only one or two sources of ground current, the taps being tripped by residual voltage.

On long taps with overcurrent protection considerable improvement in selectivity can be secured through the use of angle discrimination. On such lines fault current occurs at a phase angle which could not occur for large load currents or swings.

Industrial Interconnection

When a line is tapped to an industrial plant having generation, it is common practice to segregate essential loads for operation from the plant generator and dump others in event of a line outage. If the same line is tapped for other plants, the problem arises of separating the plant under consideration from the line under conditions hazardous to its operation. One scheme in successful use on many industrial interconnections consists of separation based on any of three indications, provided power flow has reversed and is toward the power company. The three indications are: underfrequency, undervoltage, or generator overload. Any of these occurrences, provided power flow is away from the plant, is taken as sufficient cause for separating and at the same time dumping nonessential loads so that the remaining plant load may be brought within the capacity of the plant generation.

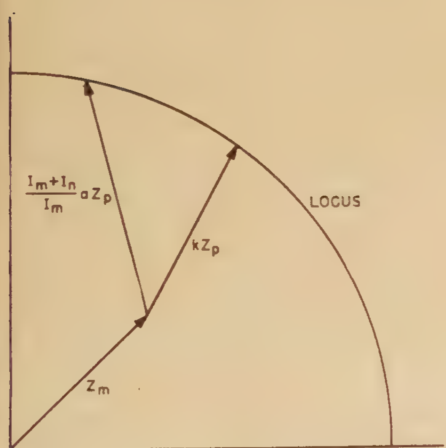


Figure 5. Graphical solution for balance point

$Z_m+\frac{I_m+I_n}{I_m}\,aZ_p=Z_m+kZ_p$

Given Z_m, Z_p, I_m, I_n (vectors) and either a or k (scalars) the other can be found by drawing the circular locus of impedance viewed by the relay

Refer to Figure 1*a* for nomenclature

Relay at *m*. Normal balance point is at Z_m+kZ_p from *m*

Balance point with mutual effect is at Z_m+aZ_p from *m*

The relays normally employed are:

Induction-type overcurrent for generator overload.

Induction-type underfrequency relay.

Induction-type undervoltage relay.

High-speed-type three-phase directional relay.

The overload relay is directional controlled so that it will not start timing unless direction has reversed.

Directional relays are also used, without the voltage, frequency, or current fault detectors, for this purpose.

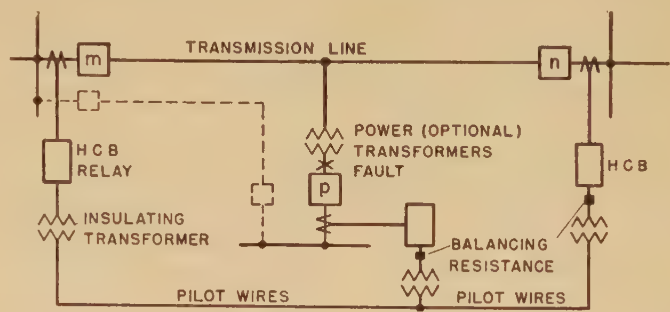


Figure 6. Typical three-terminal power circuit protected by three *HCB* relays

Impedance or Distance-Type Protection

The general principle of application of distance relays to a three-terminal line is illustrated in Figure 4. As indicated, the instantaneous elements are set to include a zone that has a radius of approximately 90 per cent of the impedance to the nearest station. Faults beyond this zone will be tripped instantaneously by the near relay and, after a time-delay period has expired, at the remote terminals. However, certain problems arise which are not present in the application to two-terminal lines.

Unequal Impedances to Remote Terminals

Even in the simplest case of one power source and no paralleling ties, the three terminal line presents a problem not present with two-terminal line protection. For example, with a source at *m* only, in Figure 1a, it is desirable to set the first zone element of an impedance relay at *m* for a large part of the line impedance to the next station. If *p* is much further away than *n*, the first zone instantaneous element can be set to trip only to 90 per cent of the distance to *n*. This leaves a considerable section of the line to *p* without instantaneous protection at *m*.

Also, the second zone impedance element at *m* must be set to trip beyond *p* in order to be sure of tripping end zone faults up to *p*. However, with this setting it may reach so far beyond *n* as to encompass another complete line section, whereas for proper co-ordination it should cut off somewhere in the first zone protective region of that next line section.

A similar problem exists with the third zone element which is set where possible to back up completely the subsequent line section.

These problems occur also in the other cases where more sources and paralleling ties are present. It is evident that considerable advantage can be secured from a pilot channel to prevent the high-speed impedance elements from overreaching either of the other terminals, as will be described subsequently.

Mutual Impedance Effect

While the impedance relay operates on the ratio of current to voltage it is convenient to hold one quantity fixed and visualize the operation as the other quantity varies. Suppose a constant fault current is considered through the relay at *m* and the fault is gradually moved from the relay toward *p*. At first there is no voltage restraint, and the relay operates. As the fault is moved further away, the voltage restraint on the relay increases directly with the distance or impedance, until a so-called "balance point" is reached beyond which the voltage restraint exceeds the current operating force. Suppose that with a single power source this normal balance point is at 90 per cent of the distance to *p* and takes in 80 per cent of the section from *t* to *p*.

If a second source is introduced at n and the fault is again moved from m toward p, the restraint again increases directly until t is passed. Then, if the source at n provides four times as much current at the first source, the restraint increases five times as fast for distances of fault beyond t, since the current producing the voltage drop is five times as great in this section. The balance point is therefore reached at 1/5th of 80 per cent or 16 per cent of the distance from t to p. Or, in general, if the normal balance point is at an impedance, Z_m+kZ_p, from the relay, with a source at m only, and no paralleling ties, the balance point moves to an impedance

$$Z_m+\frac{I_m}{I_m+I_n}kZ_p$$

from the relay when the source or paralleling tie is added.

This "backing-up" of the balance point of a relay caused by current entering at the tap point is termed "mutual-impedance effect." It introduces complications because of the variable location of the balance point depending on the amount of generating capacity connected at each terminal.

However, it may be noted that if the second zone elements are set to include double the impedance of the common branch, tripping will always be assured although it may be sequential. Any current distribution that more than doubles the common branch as viewed from one terminal m, will less than double it as viewed from the other terminal n. The terminal with the larger current will therefore trip, eliminating the mutual effect, whereupon the other terminal will trip.

Mutual Effect of Loads

Even with a single source and no paralleling ties, load currents will have some effect on the balance point of impedance relays, particularly when fault and load currents are of the same order of magnitude. In Figure 1a with a fault at p, current to the load L_n, causes mutual drop in the branch m. Since this load current flows through the relay but does not create proportional restraining voltage by flowing over the line clear to the fault, it causes an overreaching effect which must be considered particularly in the setting of a first zone element. In terms of a normal balance point at a distance, or impedance, Z_m+kZ_p, the addition of the load L_n moves the balance point to an impedance

$$Z_m+\frac{I_m}{I_m+I_n}kZ_p$$

from the relay. This expression is identical with that given previously which makes the fraction $I_m/(I_m+I_n)$ greater than one. Starting with a normal balance point at Z_m+80 per cent of Z_p, a load flowing to L_n and constituting 40 per cent of the total relay current I_m, would move the balance point to Z_m+133 per cent of Z_p. (Note: 1/1–0.4 x 0.8= 1.33.) To prevent overtripping, the normal balance point must be reduced. A balance at Z_m+aZ_p with the load present can be secured by making the normal balance point:

$$k=\frac{I_m+I_n}{I_m}a \tag{1}$$

Then, if $a=0.8$, $k=1-0.4/1\times0.8=0.48$. A normal balance point at Z_m+48 per cent of Z_p will be moved to Z_m+80 per cent of Z_p when the 40 per cent load is present.

Phase-Angle Effects

In the foregoing discussion the balance-point expression has been treated as algebraic which is, of course, strictly true only if the impedance phase angles are alike and the currents I_m and I_n either in phase or 180° out of phase. More generally the balance point occurs at Z_m+aZ_p where a can be determined by solving the equation

$$\left|Z_m+\frac{I_m+I_n}{I_m}aZ_p\right|=|Z_m+kZ_p| \tag{2}$$

A graphical solution is shown in Figure 5.

Pilot-Wire and Carrier Pilot Protection

Pilot protection using either wire or carrier channels has proved to be quite an effective solution to the problems of relaying three-terminal lines. Two common types of pilot protection will be discussed here. They are known respectively as, first, the directional comparison or blocking type which is used with d-c pilot-wire or carrier-current channels, and second, the two-wire a-c pilot-wire type.

Directional Comparison or Blocking Type

In the blocking scheme, directional fault-detector relays at each terminal trip for any faults in the tripping direction unless blocked by a signal from the far end indicating the fault to be beyond the far end of the line. This signal is usually initiated by a directional element which operates when power of fault magnitude flows out of the line at that terminal. Fault power flowing out at one terminal of a three-terminal line, to an external fault, will correctly block tripping at the other two terminals.

Such a blocking scheme is used with either wire or carrier pilot. One arrangement utilizes the step-type directional impedance relay as the fault-detecting element, and as the directional element for controlling the blocking signal, corresponding ground-current elements being used. The backup characteristics of step-distance relaying are obtained in this scheme without added impedance elements.

As previously explained, on systems with paralleling ties, as in Figure 1b or c, fault power may flow out at one terminal for a fault on the protected line near another terminal. However, with the step-distance arrangement the first zone element is used independently of carrier or pilot blocking and results in immediate tripping of the terminal nearest the fault. Thereafter fault power no longer flows out, and the carrier- or pilot-controlled relays promptly trip at the other two terminals.

Also, in extreme cases the mutual impedance effect, previously described, may prevent a tripping element from reaching to a fault at the far terminal. This may result in the blocking of pilot tripping at all terminals. However, operation of the first zone instantaneous element will in many cases open the breaker nearest the fault and remove the mutual effect. Pilot-controlled relays can then trip normally at the other terminals.

To summarize, distance-type pilot-wire or carrier protective systems can be applied to any type of three-terminal power circuit. Their operation may be slightly delayed when power flows out of one terminal of the faulted line or when mutual impedance effect prevents a

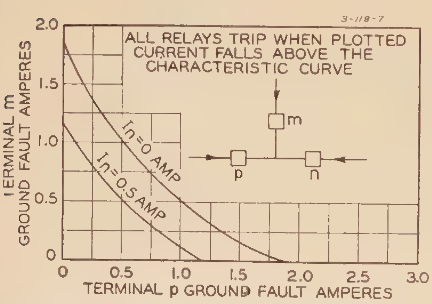
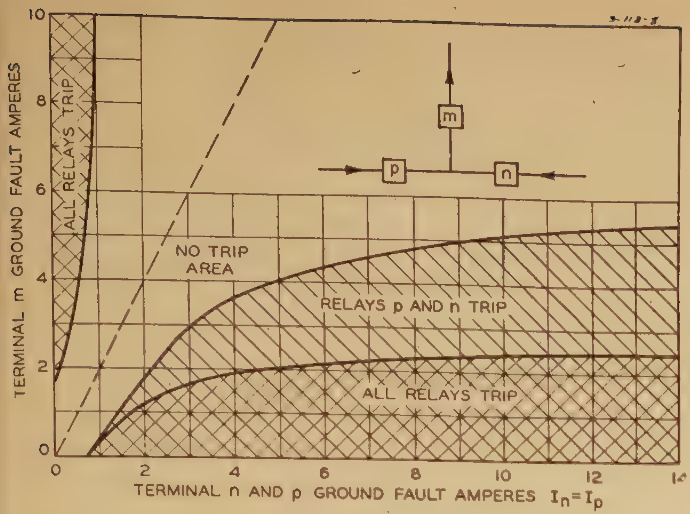
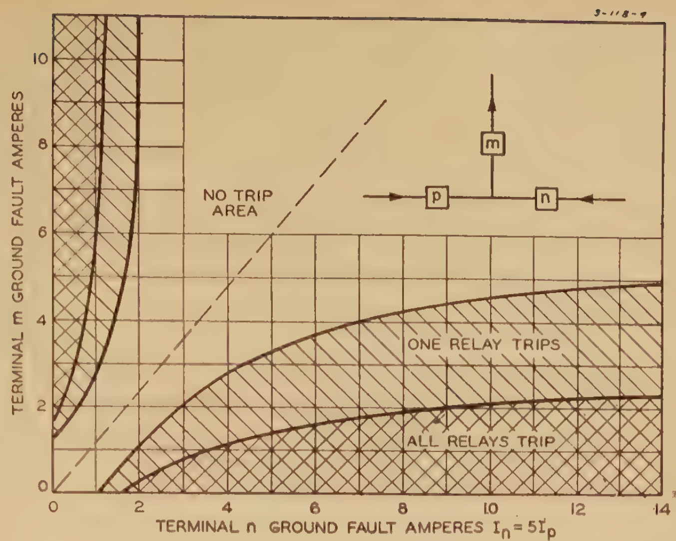


Figure 7. Typical tripping characteristic of *HCB* relays on a three-terminal line for internal ground faults

Relay set for maximum sensitivity. Pilot-wire resistance 200 ohms (each branch)

Negligible capacity between wires





Figures 8 and 9. Typical operating characteristics of *HCB* relays on a three-terminal line

Instantaneous direction of ground-fault currents indicated by arrows, all currents in phase

Relay set for maximum sensitivity (0.5 ampere ground current per terminal)

Pilot-wire resistance 200 ohms (each branch)

Negligible capacitance between wires

tripping fault detector from reaching a fault near a far terminal.

Two Wire A-C Pilot-Wire Type

The *HCB* single-element a-c pilot-wire scheme,[8] developed in 1937, which has been widely used on short two-terminal lines, is also applicable to short three-terminal lines. In fact, such applications have been in successful operation in United States and Canada since 1939. The success with these lines and the greatly increased number of such applications at the present time has stimulated a considerable study of the limits of application of this type of equipment to determine how far it can be extended. Operating characteristics have been determined as the current distribution is varied, or the length of the tapped lines increased with corresponding increase in pilot-wire resistance and capacity. Some of the significant results of this analysis are presented here.

Principle of Operation

The *HCB* scheme of pilot-wire protection operates essentially as an extended bus-protection scheme, tripping all terminals whenever the totalized current to a fault within the protected zone exceeds a predetermined setting. It has variable ratio characteristics, produced by the restraining coils and a saturation device, that makes it relatively insensitive to ratio errors of current transformers on heavy through faults. The ratio characteristics are the same in percentage for any type of fault, phase, or ground; however, the relay has separate adjustments for phase and ground currents, so that the actual magnitude of ground current required to trip can be, and usually is, set considerably lower than the phase-current trip.

It is evident from the foregoing discussion that this scheme differs from the blocking types in that flow out at one terminal will not necessarily block tripping if the total net flow is "in" to a fault on the line. It is, therefore, ideal in principle, producing immediate tripping of all terminals for internal faults.

As the number of terminals is increased above two, and as the length of the pilot wire is increased, departures from the ideal of "extended bus protection" already described begin to be observed. For example, the minimum trip value increases with the number of terminals, and some variation is noted between the fault current required to trip different terminals, depending on the current distribution and the pilot-wire resistance and capacitance.

In the *HCB* pilot-wire scheme, restraint current is circulated through the pilot wires for through-fault or normal load conditions. With three terminals the three pilot wires are all brought to a common point and paralleled, their resistances being equalized up to this common point by means of balancing resistances as shown in Figure 6. For a through fault or load current flowing in at two terminals and out the third, restraining pilot-wire currents flow from the two terminals to the pilot-wire junction where they combine to form the correct pilot current to the third terminal. For faults within the protected zone the relay voltages act in opposition over the pilot wire, reducing the restraining currents and building up voltages which pass large currents through the shunt operating coils.

The operating characteristics of the *HCB* pilot-wire-relay scheme are illustrated by the charts of Figures 7, 8, and 9. If each relay is given a ground-current setting of 0.5 ampere, all relays trip whenever the total current to a fault in the protected zone totals approximately 1.5 amperes, or 0.5 ampere per terminal. The minor variations from this approximation are shown for moderate pilot-wire lengths in Figure 7. With equal distribution 0.5 ampere per terminal trips, whereas 1.9 amperes in terminal p alone is required to trip all three relays, the difference being due to the greater restraint current under the latter condition. This difference is usually of small significance.

The characteristic curves are given only for ground current, but it should be understood that the same curves apply for phase currents if the current scales are multiplied by a constant, that is, by the ratio of phase setting to ground setting given the particular relays.

The curves of Figures 8 and 9 are used first of all to show the amount of current-transformer inaccuracy that would be required to cause misoperation on a through fault. All current entering p and n must leave at m for a through fault. The currents p and n are considered equal and plotted as abscissa in Figure 8, while the current out at n is plotted as the ordinate. The curves separate trip from nontrip areas. Thus, two amperes in each of n and p (abscissa 2), and four amperes out at m, falls on the dashed line well within the no-trip area. In fact even should the ratio error of the current transformers at m be so great that only two amperes was delivered, the relay would still not quite trip. However, dropping to 1.5 amperes would cause undesired

tripping at p and n. The burden is moderate (1.6 ohms in residual circuit) so that current-transformer errors beyond those permissible according to Figure 8 would not be expected.

It was mentioned that the relay tripped on the basis of net current flow into the protected zone and hence operated even with some flow out at one terminal. There is obviously a limit to how far this can be carried, since the greater the departure from equal feed in at all three terminals the greater the restraint current flowing and the higher the operating value. With sufficiently high current out at one terminal, tripping will be prevented. Figure 8 is also intended for determining whether correct tripping will be obtained during an internal fault with maximum flow out at one terminal. It shows for example that if two amperes (secondary) flow in at each of p and n terminals, as much as 1.0 ampere leaving at m will not block tripping.

Curves similar to Figure 8 have been prepared for other than equal values of currents at p and n. Figure 9 shows the corresponding characteristic with the current at n equal to five times that at p.

It has been found from experience with a number of applications that the flow out is not sufficient to block desired tripping, as determined from data such as Figures 8 and 9. It can be concluded, therefore, that in practical cases, the *HCB* relay as used on three-terminal lines provides instantaneous tripping for all internal faults on three-terminal lines. Also it provides adequate margin to prevent operation on through faults even with quite large current-transformer inaccuracies. As mentioned, a number of installations have proved this to be a reliable means of obtaining high-speed (one cycle) relay protection on three-terminal lines. Standard equipment is used so that existing circuits frequently can be expanded simply by adding a third relay.

The pilot wires used should preferably be enclosed in cable sheaths, protected from lightning, induction, and differences in station ground potentials so as to maintain a continuous circuit at all times.[5,6] On three-terminal lines the shunt capacity between pilot wires should not exceed 0.5 microfarad per terminal for 500-ohm circuits. Continuous d-c supervision can be supplied with the relay to ring an alarm whenever any branch of the circuit is opened or faulted.

Conclusions

The co-ordination of directional overcurrent and distance-type relays is more difficult in three-terminal lines because of two principal problems.

1. Unequal impedances to remote terminals.

2. Mutual impedance effects from fault and load currents entering or leaving at the tap.

Blocking-type pilot schemes assist in solving these problems, but on three-terminal lines with paralleling ties undesired blocking may be produced by:

3. Power flow out at one terminal to a fault within the protected zone near another terminal.

The first zone element of step-distance-type pilot schemes solves this problem by operation independent of the carrier-current or pilot-wire blocking, but at the expense of a slight time delay.

The *HCB* two-wire a-c pilot-wire scheme circumvents all of these problems and provides uniform high-speed protection even in the more difficult cases of three-terminal line protection. Typical operating characteristics for three-terminal line operation are presented in the paper.

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Arc-Furnace Control by Regulex Exciters

T. B. MONTGOMERY
MEMBER AIEE

T. H. BLOODWORTH
ASSOCIATE AIEE

FOR a number of years prior to the present urgency for the production of high-quality steel, the development in control methods for electric-arc furnaces remained substantially stationary in this country. These devices were in the general form of vibrating regulators or contact-making ammeters which rapidly connected the electrode motors to and disconnected them from a constant d-c voltage source. In Europe, meanwhile, various methods employing Ward-Leonard or hydraulic control means were developed, some of which were reported to result in smoother operation under the highly fluctuating transient conditions of the arc.

Some six months prior to Pearl Harbor, study indicated that with stepless regulating or control means available in this country improved results might be effected. Such means consists of a rotating regulator in the form of a Regulex exciter. The first of this type control, here discussed, has now been in operation long enough for a definite judgment of operating results.

Qualities Required in a Regulator

1. Actuation from a measure of arc or heating energy.

2. Correction increasing in value as deviation from normal increases and decreasing to zero as deviation decreases to zero.

3. Quick response.

4. Stable antihunt means to prevent overshooting.

These requirements, of course, need to be met in any successful closed control system for governing or regulating. However, in the arc furnace the regulating problem is unusual because of the high degree of instability of the electric current and power in the arc.

By referring to A and A^1, Figure 7, film 2, it will be seen that fluctuations occur about every six cycles[1] near the beginning of a melt in a steel furnace. This is due in part to rapidity of changes in ionization, varying degree of rectification, and so forth. Obviously, it would be impractical to move an electrode system weighing perhaps a ton, in a six-ton furnace, fast enough to correct for these variations by control of arc length. Cur-

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T. B. MONTGOMERY and T. H. BLOODWORTH are both with the Allis-Chalmers Manufacturing Company. Mr. Montgomery is engineer-in-charge, control engineering, switchgear and control division in the electrical department of the company at Milwaukee, Wis., and Mr. Bloodworth is engineer in the control section of the company at West Allis, Wis.

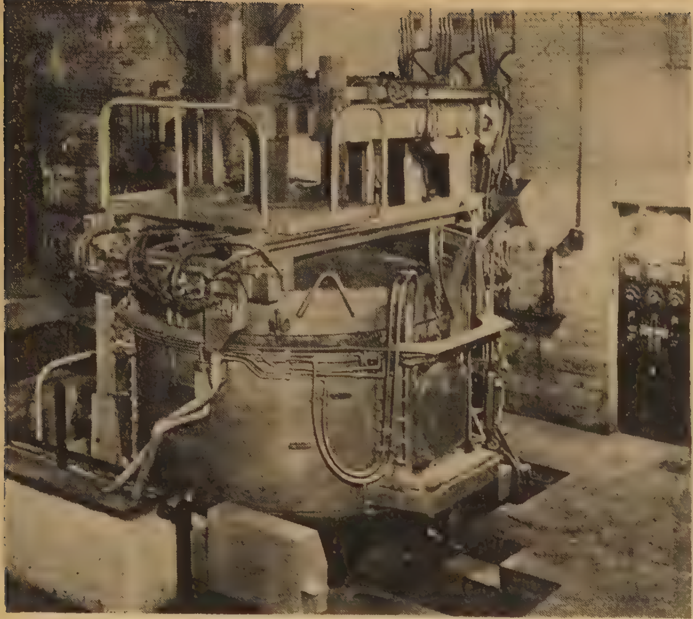


Figure 1. Six-ton top-charge arc furnace with operator's panel at right

rent is limited in such transients to a safe maximum value by transformer and lead reactance and where necessary an external reactance, as $X2$ in the equivalent circuit of Figure 3. But, under the same conditions, the variations in average values, from maximum to minimum as from A to B, Figure 7, film 2, occurs every one to two seconds. This is due in part to metal melting away, electrode melting, and so forth, which changes the arc length.

Thus a good regulating system must hold the average value to minimum variation. The Regulex exciter was selected for these requirements.

Rotating Regulators

Amplification

In a d-c generator rotating at constant speed, such as Figure 2A, the output power is controlled by the excitation winding CF. The machine is recognized as a power amplifier, and the control power in winding CF is a small percentage of the power output. This percentage is two per cent to five per cent in a machine of standard commercial design giving an amplification factor of 50 to 20, respectively. This ratio is dependent on frame size and design, and in general the larger the frame size the greater the amplification factor for a given output voltage because the larger the frame the more flux per watt excitation is set up. Furthermore, by connecting two such machines in tandem as in Figure 2B the ratio of power output to control power is the product of the amplification factors of the individual machines. Therefore, with a given control power available, in the form of a measure of the quantity to be controlled, a wide variety of amplification factors are available to the engineer to obtain the necessary power output. The power output then is used to react on the quantity to be controlled whenever it varies from normal.

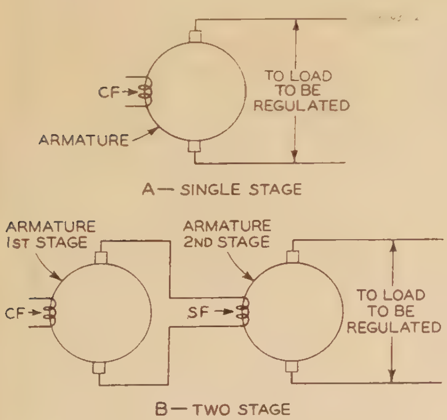


Figure 2. Regulex exciters

For example, in Figure 3, the control power available is current-transformer five-ampere metering and arc potential. Field 4 is designed for current-transformer metering current, and field 5 is designed for corresponding volt-ampere and ampere-turn capacity. The Regulex exciter 3 is the first stage of the amplifier and is of sufficient size to excite the electrode generator 2, which is the second stage. The second stage then is made of suitable capacity to drive electrode motor 1. This motor acts to change arc length when arc current and voltage vary from normal. Obviously for small electrode motors only one generator need be used.

Stability

The next most important consideration is stability of the system to prevent overtravel and "hunting." In the Regulex arc-furnace control, the actuating medium in the system is the resultant excitation of two opposed fields, 4 and 5, Figure 3, in the Regulex exciter. The control power is the sum of the excitation watts of these two fields. These fields are equal in ampere turns at normal value of arc watts or heating energy (the aforementioned quality 1 required in a regulator). No actuating flux being present, under this condition, no voltage is applied to the electrode motor.

Curve B, Figure 4, gives a typical volt-ampere arc characteristic. The change in voltage is not linear with change in current but is sufficiently large to satisfy quality 2. For small deviations from normal the actuating flux is essentially proportional to the deviation of arc watts from normal and the resultant power output to produce correction is essentially proportional to this deviation. This produces an inherently stable system where mechanical and electrical inertias can be neglected, thus signifying quality 4. Where these factors are appreciable, an anticipation of "cutoff" of the actuating flux proportional to such inertias is required. The amplification together with design for low inductance in all circuits satisfies quality 3 as will be later shown.

Sensitivity

The sensitivity of the system depends on amplification ratio, friction in the electrode system, and hysteresis in the electrical machines. As will be seen from the volt-ampere characteristic, curve B, Figure 4, at very low currents the change in arc volts is greater per unit change in current than at higher current values. For this reason good sensitivity is maintained inherently down to very low currents. The system is quite stable at 10 to 15 per cent current which is necessary to prepare furnaces for operation after relining (burning bottom).

Arc-Furnace Control

An arc-furnace installation is subject to greater fluctuation in power and surges[2] than other installations requiring control because of:

(*a*). More frequent switching.

(*b*). Wide range in primary transformer taps to provide necessary secondary voltage.

(*c*). Large reactance to limit maximum current.

(*d*). Varying amount of d-c component in the arc.

(*e*). Rapidly fluctuating state of ionization in the arc.

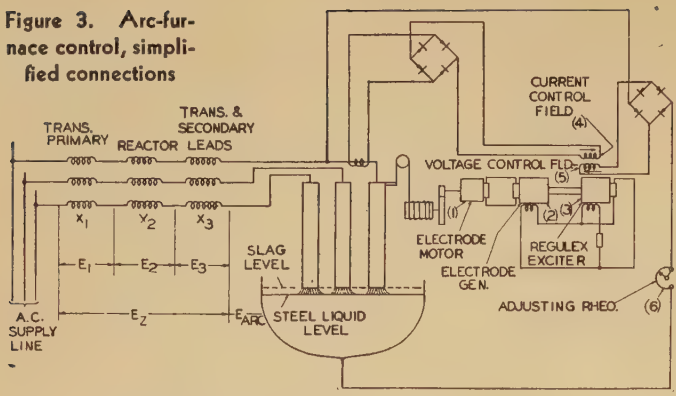


Figure 3. Arc-furnace control, simplified connections

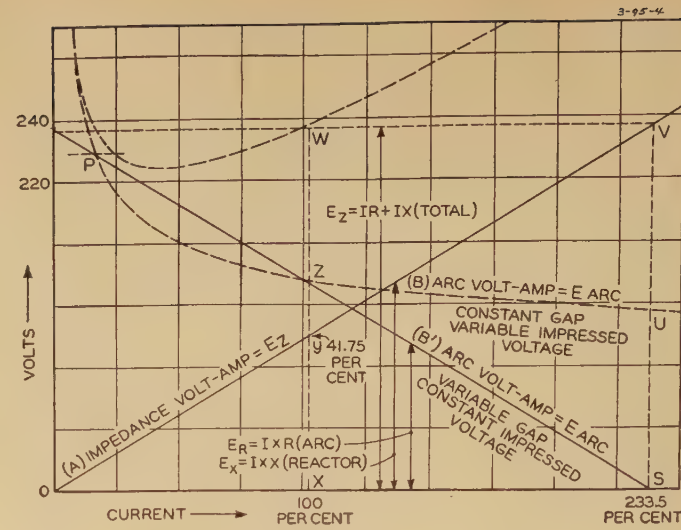


Figure 4 (right). Typical arc-circuit characteristics

The last item *e* has greatest effect from the control standpoint.

The complete system, here under consideration, consists for each electrode, of a Regulex exciter controlling a generator of suitable size with its armature connected electrically directly to the armature of an electrode motor which raises and lowers the electrode. In addition, there are the necessary control switches, meters, indicating lights, and so forth. This equipment is duplicated for each electrode. For a three-phase furnace three Regulex exciters, three genrators, and an a-c driving motor form a small seven-machine motor generator set. The complete connections are shown in Figure 5. The motor generator set, as shown by the connections, may be supplied from the furnace bus, thus making the entire installation self-contained, and not dependent on a separate d-c power source. Figure 6 shows the control equipment from the rear of the operator's panel. The panel at the left mounts, plate-type rectifiers, control resistors, and so forth. On small electrode motors, generally under one horsepower, the Regulex exciter armatures are connected directly to the electrode motors, thus resulting in a small four-machine control unit.

As shown in Figure 5, current settings in each phase are made by rheostats *R*1, *R*2, and *R*3 in the arc-potential circuit. By changing, for example, the excitation of *VF-A*, the phase current is automatically changed, since the differential flux between *VF-A* and opposed current field *CF-A* produces voltage to run the electrode in the proper direction (raise) until the fields are equalized.

Four voltage positions normally are provided on the transformer tap changer. On the highest voltage all of resistance *R1A*, *R2A*, and *R3A* in the potential circuit are connected in the circuit. When the circuit is changed to the next lower

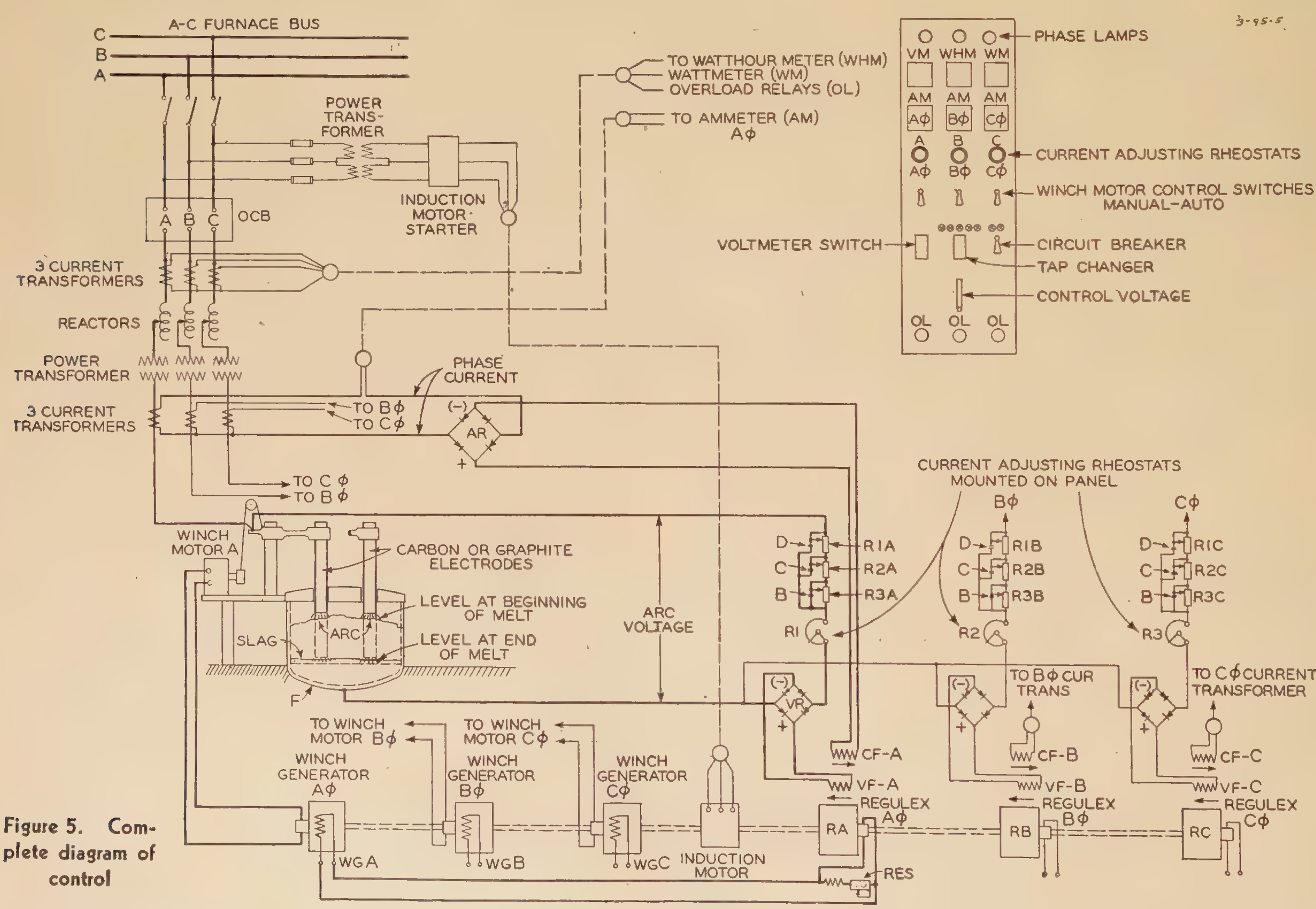


Figure 5. Complete diagram of control

voltage, contact B automatically short-circuits resistor $R3A$ so that on the new voltage the current setting is not changed. Contacts of B also perform the same function on the other phases, and for other voltages contacts C and D act in like manner.

Operation of the Control in Service

A good image of the net operation of the control can be obtained from examination of oscillograms taken with a furnace in operation. The films of Figures 7 and 8 were taken on a steel furnace of six tons nominal charge capacity. Arc energy is supplied through a furnace transformer from a 22,000-volt three-phase 60-cycle line. The analysis of the melt was

Carbon	0.35
Manganese	0.14
Silicon	0.43
Phosphorus	0.035
Sulphur	0.037

In all films, which were taken on a single phase, the oscillogram traces starting at the top of the film and progressing downward measured the following:

1. Arc volts.
2. Electrode (or winch) motor, armature volts.
3. Arc amperes.
4. Voltage-control-field amperes (rectified).
5. Current-control-field amperes (rectified).
6. Electrode (or winch) motor armature amperes.

All values are rms except rectified Regulex-control-field currents which are peak values.

In film 2, Figure 7, taken 15 minutes after the start of the melt, just prior to sudden current increase, at A, the electrode motor was operating at 75 volts and lowering the electrode at 10.85 inches per minute. With the sudden current increase the motor was reduced to zero speed in 26 cycles. However, it was not decelerated at a constant rate, as can be seen from the motor armature current. As the average difference in current and volts decreased, the rate of deceleration decreased. The motor reached practically zero speed 26 cycles later and remained so for approximately 35 cycles up to point C. At this point the average arc energy had dropped below normal, and the motor accelerated for 35 cycles until it had 90 volts across its armature. At this point the average arc energy became essentially normal, and the electrode speed remained at 14.35 inches per minute lowering, and the motor armature current ran to approximately zero.

As will be shown later, between points E and B, mutual inductance between the two opposed control fields prevents response to the peak values of only a cycle or so duration. However, following point B, the current rose sharply from about 5,000 amperes to 18,400 amperes, and the motor was decelerated to zero speed in about 10 to 11 cycles and reversed. Here a high "pump-back" current is regenerated in the electrode-motor armature.

Consider the action at point F. Prior to reaching this point because of too low arc energy, the electrode motor had been accelerated to lower the electrode until 106 volts were across its armature. The increase in current at this point, with little decrease in voltage starts decelerating the motor. However, as previously mentioned, at this point in the heat, metal is running away from the electrode as it melts, thus tending to increase arc length inherently and to reduce current. At the same time the electrode is lowering. Consequently, the rate of lowering the electrode decreases only until the average *actuating measure* of arc watts is reduced to normal which is not necessarily *zero speed of the electrode.* In examining the electrode-motor armature volts for the entire length of film 2, the electrode is raised four times, with only slight movement each time. The total area under all of these raising voltage swings below zero, compared to the total area under the lowering loops above zero, shows that the electrode is lowered through the scrap at such a rate as to maintain as near constant average heating value as the response of the system to the extreme fluctuations will allow.

Film 4, the lower one in Figure 7, was taken 65 minutes after the start of the melt of film 2. Much of the steel has been melted down, and conditions are steadier. The film speed is such as to show the general wave shape of current and voltage. At point H on the film, it will be noted that current jumped from 5,240 amperes to 13,100 amperes on successive peaks. However, the rectified-voltage control-current peak increased on this half cycle, because of mutual inductance, even though the voltage was sharply reduced. But the average of the next four half cycles is reduced from the previous four half cycles with a net small response from the Regulex unit. This characteristic is of particular value in refining at the end of the melt to prevent false movement when the metal is "boiling." When a bubble rises against the electrode and disappears within a cycle or two, after bursting, no movement of electrode results.

Film 8 at top of Figure 8, was taken one hour and 55 minutes after the start of the heat of films 2 and 4 and consequently during the refining period. Iron ore and

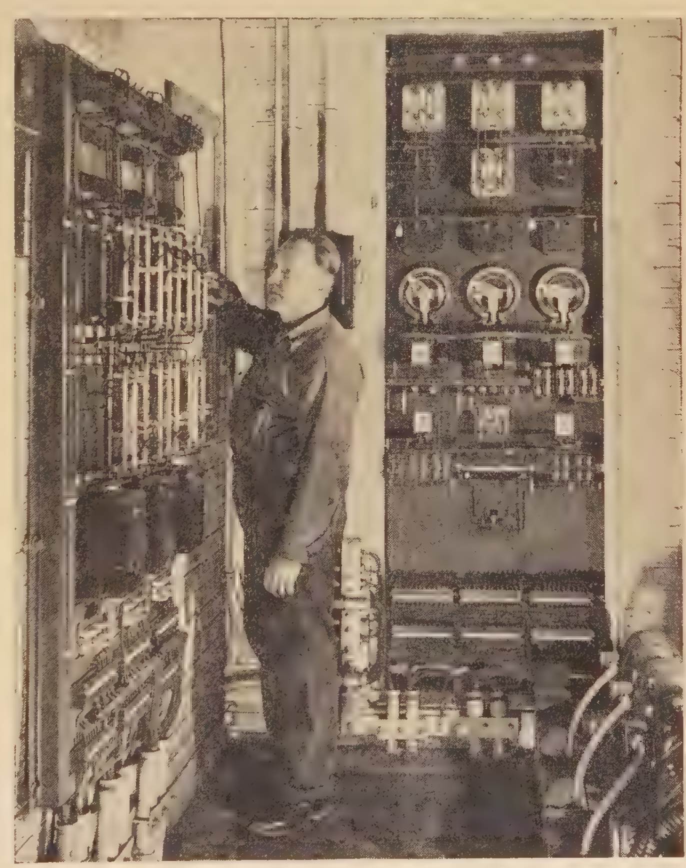


Figure 6. Rear view of operator's panel showing auxiliary panel and Regulex exciter motor generator set

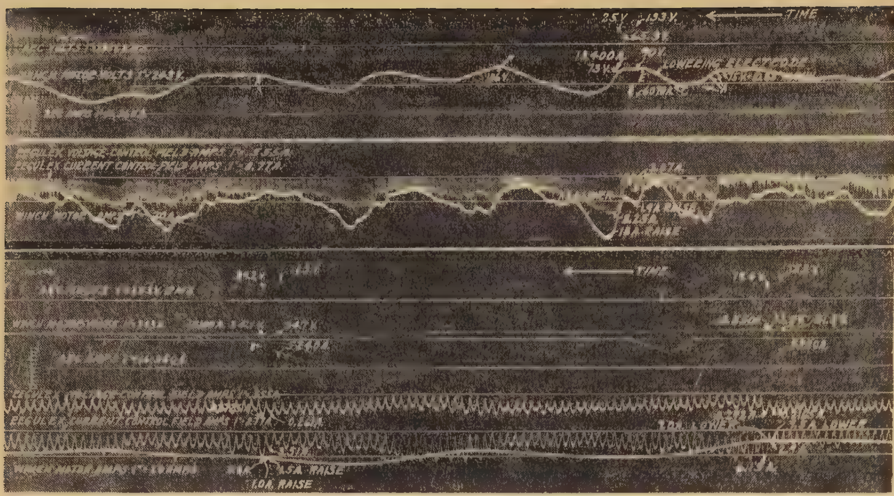


Figure 7 (above). Film 2 and film 4

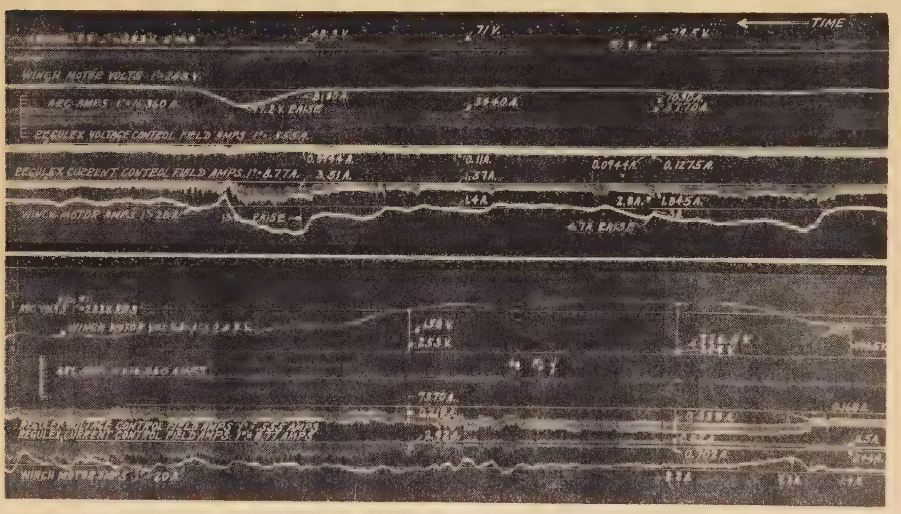
Film 2 taken 15 minutes after start of melt. Transformer on *A* tap, 205 volts phase to phase, 14.3 per cent impedance. Time 10:15 p.m. Film speed approximately $1^1/_4$ inches per second. Heat 21071. Saturday, November 21, 1942. Charge 2,000 pounds turnings, 9,600 pounds gates, 6,400 pounds plates (nine tons total)

Film 4, same heat, taken 65 minutes after start of melt. Time 11:05 p.m. Transformer on *A* tap, film speed approximately 23 inches per minute

Figure 8 (below). Film 8 and film 11

Film 8, same heat taken one hour 55 minutes after start of melt, 11:55 p.m. Transformer tap *C*, 127 volts phase to phase, 9.4 per cent reactance. Film speed approximately $1^1/_4$ inches per minute

Film 11, heat 21075. Sunday, November 22, time 3:10 p.m. (furnace shut down approximately 7:00 a.m. Sunday morning, restarted 3:10 p.m.). Start of melt. Transformer tap *A*, 205 volts phase to phase, 14.3 per cent reactance. Film speed approximately $1^1/_4$ inches per second. Same scrap and analysis as on film 2



other elements had been added shortly before and created violent boiling sufficient to necessitate a cutoff of power to prevent boiling over. The film shows regulation just after reapplication of power after boiling had subsided somewhat.

Film 11 was taken at the beginning of a heat next day. The control was purposely set so that when it was placed under automatic regulation to lower electrodes onto loose scrap, one electrode would strike the scrap in advance of the one being recorded. At point *I* to the right of the film when the electrode touched the scrap placing full-phase voltage between electrodes, the electrode motor speed was increased correspondingly. At points, *J*, *K*, *L* when the electrode first came in contact with the loose scrap, the arc was "struck" and broken some eight times before being firmly established. Thereafter, it was not lost for the duration of the heat except for interruptions by the operator for tap changing, and so forth.

It is to be noted particularly how electrode motor speed was reduced as the arc was established, until at the extreme left of the film it has reached a speed comparable to the lowering speeds of film 2. Also of interest is the low magnitude of arc current when the arc is initially established.

Power Consumption

Power consumption is, of course, dependent on many variables including type of material melted, method of feeding material into the furnace, use made of the furnace such as melting or refining, and so forth, so that kilowatt hours per ton vary from one installation to another. The control is only one element in any comparison. However, on furnaces of the same type and rating, in the same plant, evidence of more uniform resultant heating has been reflected in more heats per day as compared to older methods of control. Figures are not given because the results are unexpectedly favorable and need verification in other installations to be put into service, where comparisons with previous equipment can be made.

Effects on Power System

Previous papers[3] before the Institute have shown that maximum disturbance on the power system occurs when the arc is made or broken. From film 11, Figure 8, and the other films, it may be seen that once the arc has been established firmly, at the beginning of the melt, it is not broken again before the end of the melt, except for interruptions for voltage change, and so forth.

Also, it has been found that a lesser reactance may be used than with older methods, thus improving power factor to some extent with less net voltage disturbance. Comparative data are not available on this general comment.

Conclusions

1. A stepless direct acting regulator is readily adaptable to highly fluctuating electrical quantities.

2. Application of such control to arc furnace has improved efficiency and reduced power consumption and maintenance.

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Aircraft Electric Power-Supply System

J. E. YARMACK
MEMBER AIEE

Synopsis: An ever increasing demand for electric power in aircraft has been felt for some time. The necessity of using an electric storage battery started the practice of accompanying it with a d-c generator, first of low current and voltage capacity, later of 50-ampere 12-volt rating, and for the past few years of 200-ampere 24-volt output. In the largest four-engine aircraft, even four generators of maximum capacity do not furnish ample electric energy. Experiments have proved so far that generation of a-c power and application of lightweight transformers, together with its conversion to direct current by means of selenium rectifiers, is entirely a reliable and practical way of attaining large, trouble-free electric plant capacity in aircraft. The subsequent pages and illustrations portray the current progress made with lightweight power transformers and rectifiers in the a-c–d-c aircraft systems. They also represent at least a partial answer to the questions raised by Lieutenant Colonel Holliday[1] when he discussed problems of applications of electric power in aircraft two years ago.

THE development of a-c–d-c power systems for aircraft application started with a view of achieving two main objectives:

1. Augmenting the source of electric energy offered by conventional d-c generators.

2. Alleviating the difficulties of commutating machinery and voltage limitation under conditions of high-altitude operations.

Furthermore, having alternating current as a primary source of electric energy, some of the a-c powered equipment in the aircraft can readily be used with or without resorting to transformers, whereas d-c operated devices may be powered through rectifiers with or without transformers.

The first stage of early development was to duplicate the largest d-c generator of 200-ampere rating, operating the so-called 24-volt system, with the output voltage set at 28.5 volts in order to maintain the proper charging rate of the battery.[2] The weight, efficiency, and, to some extent, the bulk of the generator were to be at least duplicated or, if at all possible, were to be met more favorably with a combination of alternator and rectifier.

The paramount requirements of the aircraft power rectifier narrowed down to the following three:

1. High efficiency.
2. Minimum weight.
3. Satisfactory service.

An efficiency of 84 per cent appeared to be feasible and acceptable. A unit of six-kw rating, with suitable enclosure and connecting terminals, would receive favorable consideration if its weight could be in the neighborhood of 15 pounds. The larger-sized units are to be a multiple of this one. Satisfactory service could only be expected if the performance of the rectifier is:

1. Fairly uniform under extreme temperature, humidity, and altitude conditions.

2. Unaffected by vibration.

3. Capable of maintaining good operating characteristics for at least 500 hours if not longer and with a reasonably small amount of air-blast cooling.

The transformer, on the other hand, from the very start presented one important problem, that is, the attainment of the lightest possible weight. The air-blast cooling was set out to be a definite means of meeting this requirement. As it happened, however, the latest kind of material and the unique design produced very gratifying results.

The requirement, design, and performance factors of the alternator constitute an ample subject for their own discussion. It is sufficient to state here that even the early alternator, powering a 200-ampere 30-volt rectifier, was 26 pounds in weight and 90 per cent efficient. Several selenium rectifiers were designed, built, and tested under low and high temperatures, rarefied air, and excessive vibration conditions. Their performance was in every respect satisfactory and, therefore, promising.

In order to evaluate fully the soundness of higher power generations,[3] plans were drawn to build an aircraft electric system capable of delivering a d-c output of 800 amperes, 30 volts, under varying temperature and humidity conditions and with a reasonably small amount of air-blast cooling. Such a system, Figure 1, comprising alternator, transformer, and rectifier, was built and tested.

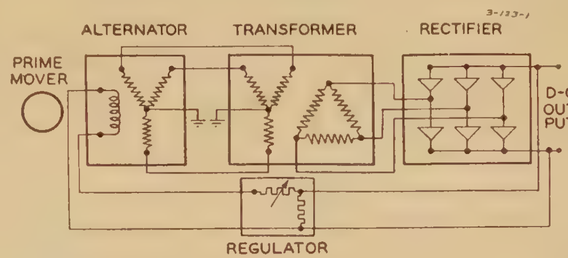

Figure 1. Block diagram of aircraft electric plant of 800-ampere 30-volt d-c rating

Rectifier

Even during early experiments with a 200-ampere rectifier, it became evident

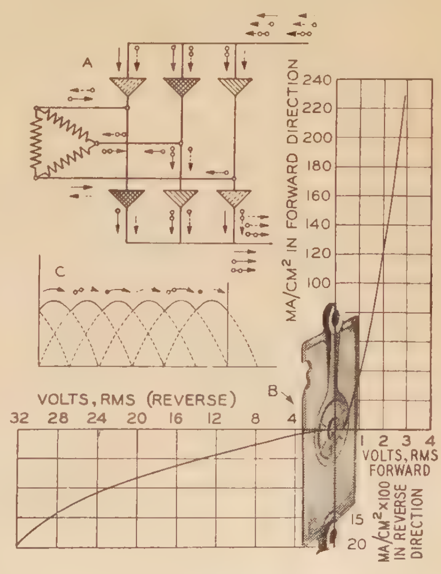

Figure 2 (right). High-voltage selenium rectifier plate

A. Diagram of three-phase bridge circuit and distribution of current in its six arms

B. Operating characteristic, forward and reverse voltages in rms values, and the currents in arithmetical values, as they function in three-phase bridge circuit. Cut shows four layers of rectifying element: back electrode, selenium layer, barrier layer, and front electrode

C. The net result of performance of the six arms of the bridge circuit during each cycle

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J. E. YARMACK is commercial engineer, selenium rectifier division, for the Federal Telephone and Radio Corporation, East Newark, N. J.

The author wishes to express thanks to the officers of the United States Army Air Forces, engineers of the Chrysler Corporation, and to the colleagues of his own organization for their co-operation in carrying on these experiments.

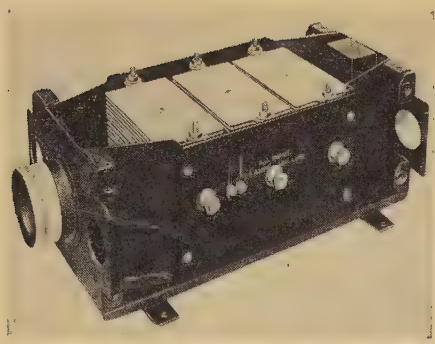
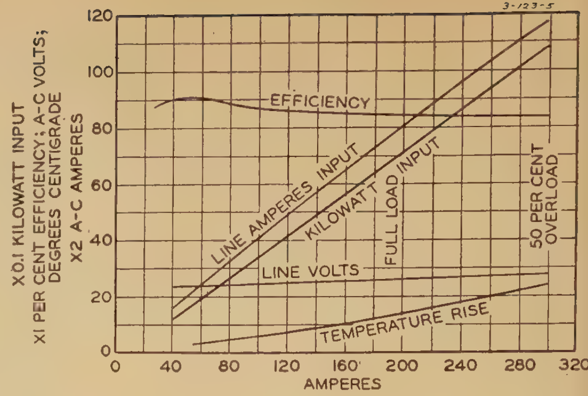


Figure 3. Aircraft rectifier, three-phase input, 200-ampere 30-volt d-c capacity

Top and side covers removed. Weight 15 pounds. Rectifying elements occupy less than 0.25 cubic foot and weigh 11 pounds

Figure 5. Operating characteristics of rectifier unit shown in Figures 3 and 4

Rated at 200 amperes, 30 volts, at ambient temperatures from minus 70 to plus 50 degrees centigrade. Unit is capable of withstanding 50 per cent current overload continuously for ten hours



that if selenium rectifiers were to play a role in 24-volt aircraft systems, the design and technique of the manufacture of selenium plates had to be modified at least to the extent of using a single selenium plate in series. Such a plate had to be of at least 28-volt rms rating and still of the lightest possible weight.

This plate, as finally developed, weighs $1^1/_4$ ounces bare and $1^3/_4$ ounces when equipped with the necessary hardware including fuse, Figure 2. In the three-phase bridge circuit, selected as most suitable for the aircraft power conversion, this selenium plate accounts for 12.5 amperes of the entire current output at 30 volts d-c with 350 cubic feet of air per minute. It is fitting here to once more reiterate the function of this type of rectifying element. The phenomenon of electric current rectification wholly takes place in the barrier layer which, in this particular plate, has been strengthened in the reverse direction but with the minimum adverse change in the forward direction. The static characteristic of a metal rectifier is determined by applying, first, d-c voltage in the forward direction (from zero to one, two, or three volts) and recording the d-c current per unit area (milliamperes per square centimeter); and second, by applying d-c voltage from zero to maximum plate voltage in the reverse direction and measuring the current leakage in milliamperes per unit area.

There is a total of six arms in the rectifier of the three-phase bridge circuit, but only two arms function during each one sixth of a cycle. The remaining four arms are blocking, Figure 2*A*. Since the total d-c output is greater than 12.5 amperes, several plates are placed in parallel. The rectifier of 200-ampere 30-volt rating has a total connection of 6–1–16, and the figure 16 designates the number of parallel plates in each arm of the bridge circuit. Physically all 96 plates are assembled into three stacks, interconnected with suitable bus structure and are housed in a lightweight case designed and built in accordance with usual aircraft practice, Figures 3 and 4.

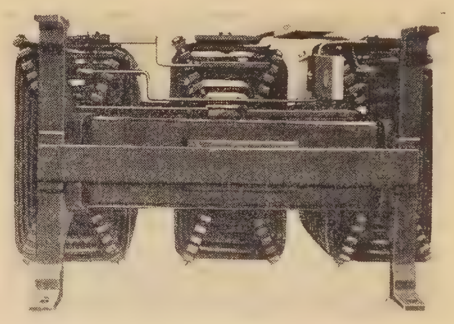


Figure 6. Aircraft transformer, air-cooled, 33.7-kva 400-cycle three-phase, 250-volt primary winding, 28-volt secondary winding

Weight 30 pounds. Front view. Over-all length ten inches

Figure 4. Aircraft rectifier, same as Figure 3, side covers open, ready for installation of conduits and cables

The upper section of Figure 2*B* illustrates the function of the selenium plate in the passing or forward direction. There is a certain amount of voltage drop per plate. This drop varies with the current density or the total output of the rectifier and constitutes forward losses. In a rectifier giving an output of 200 amperes, its value is in the neighborhood of 2.3 to 2.7 volts. This drop, however, changes with the time of service of the rectifier. Its increase during 500 hours of continuous operation may bring a net result of five per cent drop in the d-c output with the same a-c input to the rectifier. For some time, it had been expected that an idle condition of the rectifier would give a marked increase in this voltage drop. To date, however, observations have shown that this is not the case, although some momentary deforming of selenium plates may be expected after long idleness.

With the two arms of the bridge circuit passing the current in forward direction, the remaining four arms are blocking. An ideal rectifier would block reverse current completely. Actually, however, there is a certain amount of current leakage. Its value depends on the operating temperature to some extent but mainly on the length of service of the rectifier itself. The lower curve of Figure 2*B* illustrates a typical leakage current characteristic at an ambient temperature from 10 to 40 degrees centigrade. At both ends of this temperature range, the leakage current increases. Under all temperature conditions, however, the currrent leakage decreases with the life of the rectifier. The curve represents a typical reverse voltage-current density

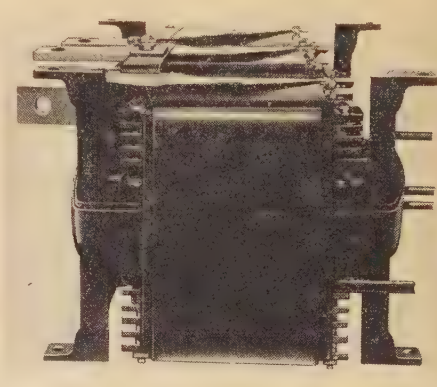


Figure 7. 33.7-kva three-phase air-cooled transformer, same as Figure 6

Side view. Over-all width seven inches

relation observed with two plates opposing each other. In the actual three-phase rectifier circuit, the reverse current is somewhat higher than that shown in Figure 2*B*, since the effects of all plates are integrated over the entire cycle. Computations and subsequent tests showed that the total current leakage of a 200-ampere rectifier is approximately 2.4 amperes at the start of service and is in the neighborhood of 1.6 amperes after 500 hours of continuous operation.

The necessary a-c input voltage to give the required direct current output voltage is computed by means of already established formulas.[4–6]

$V_{ac}=0.74\times 30+2\times 1\times 2.7=27.6$ volts

The performance of this rectifier is illustrated in Figure 5.

Transformer

In the design of the transformer, full advantage was taken of improved materials and latest methods. Following the general aircraft practice of obtaining minimum weight and highest efficiency, high-permeability low-loss silicon steel was selected for the core structure.[7,8] Since the transformer was to operate under conditions of high magnetic induction, this type of steel, insulated with a silicate glass and processed under a rigidly controlled technique of heat treating and crystal orientation, was to give maximum amount of flux, some 40 per cent more than the best transformer steel, with a remarkably small external magnetizing force. Furthermore, this material permits not only lower total losses, including copper losses, but also their better balance, greater short-time overload capacity, lower impedance, and better voltage regulation. The latter measured 12 per cent and with the flux density of 16,150 lines per square centimeter and resulting core losses of 320 watts at 350 cycles per second, the efficiency of the 33.7-kva transformer was 95 per cent at full load. As the frequency increases, the core losses decrease. Even with the frequency 25 per cent higher, when the flux density was in the neighborhood of 12,800 lines per square centimeter, the core losses decreased almost 40 per cent. At these high frequencies, the machined butt joints also added to the high performance of the transformer.

Both windings were designed with a view to keeping copper losses to the minimum. This has been accomplished with the use of a rectangular wire for the primary windings and with the use of very thin and wide flat copper strips for the secondary windings. In this manner, the radiating area of the copper was increased markedly, and, since there is a certain amount of skin effect in the conductor under the conditions of high frequency and high current, this feature helped greatly in keeping copper losses down. With the current density of 6,600 amperes per square inch in the primary winding conductor and some eight per cent less in that of the secondary, the copper losses at 75 degrees centigrade were only 620 watts and 610 watts for primary and secondary windings, respectively.

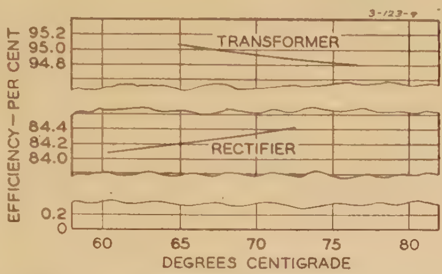


Figure 9. Characteristics illustrating the effect of temperature upon efficiencies of transformer and rectifier

All other material employed in this transformer was also selected with the sole objective of obtaining optimum performance. The winding, for instance, is separated from the core by means of heat-resistant glass cloth-base laminated plastic. The ventilating ducts in the winding are formed by means of sticks made from the glass cloth-base plastic. The frame itself is made of four corner supports welded from pieces of high-strength aluminum alloy. The supports are clamped to the core with a steel tape in such a manner that vibration stresses are taken by the supports rather than by the entire transformer structure. This type of frame construction contributed greatly in achieving lightness of weight, Figures 6 and 7.

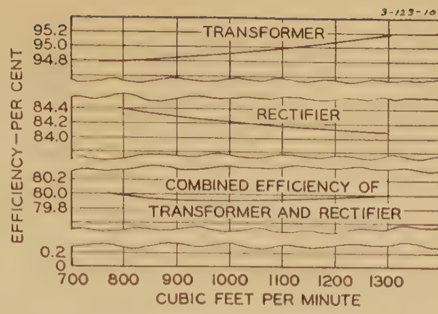


Figure 10. Transformer and rectifier efficiency characteristics as affected by the amount of cooling air

The secondary winding, carrying approximately 375 amperes, is spirally wound and has a-c resistance much lower than the conventional type of winding, The actual amount of 650 watts of copper losses in such a winding at 1,000 cycles per second frequency and 150 degrees Fahrenheit operating temperature would be some six times as great if the same winding were made of a square wire, of the identical cross-section area and arranged in a single layer. Furthermore, the copper-foil winding facilitates cooling, since the thin and wide strip has a surface area of almost six square inches per inch of conductor length, whereas, the square wire conductor has a surface of one square inch per each inch of conductor length.

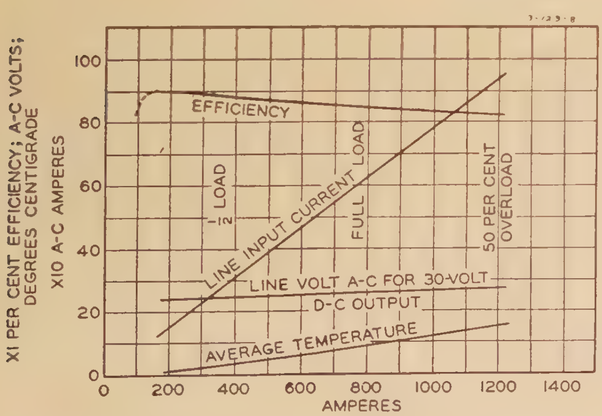


Figure 8. Performance characteristics of 800-ampere 30-volt rectifier used in Figure 1 system

Alternator

During the past year, several alternators were designed, built, and tested. Two voltage ratings (250 and 28 volts, line) were tried. The alternator used in Figure 1 experiment was of 35-kva 250-volt rating. Two more alternators became available. One is 16 kva to operate with the rectifier delivering 400-ampere 30-volt output; another of still lower kilovolt-ampere rating, Figure 11.

This aircraft alternator is rated at

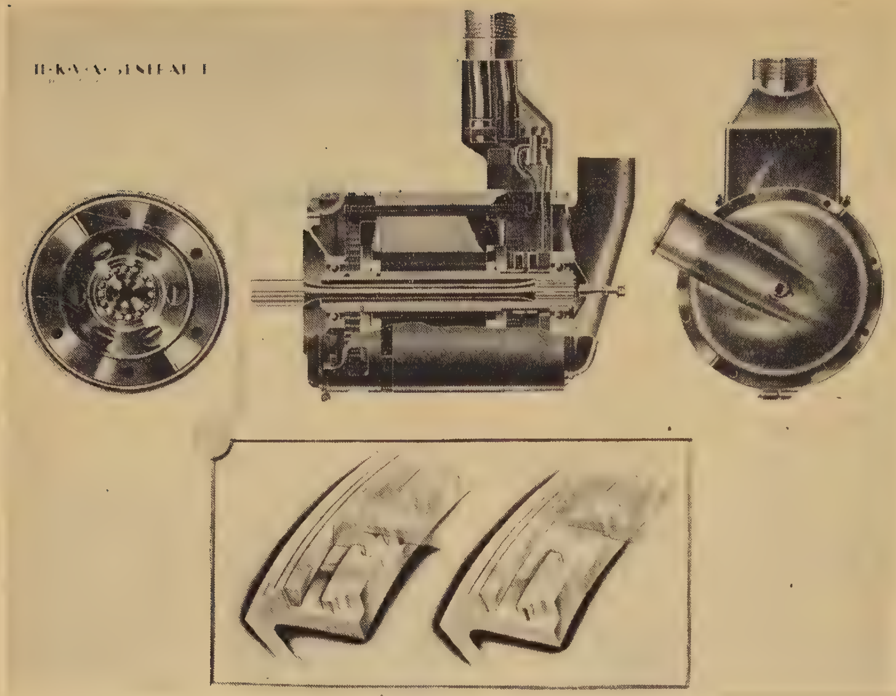


Figure 11. 11-kva three-phase air-cooled alternator, weight 33 pounds

Top, from left to right:

1. Engine pad view of the alternator
2. Section through alternator
3. Cooling cap and terminal block

Bottom, enlarged details of bayonet-type mounting flange. On left, alternator initially placed on flange. On right, alternator in locked position

11 kva, three phase, 30 volts, 211 amperes, 440 cycles and is capable of delivering 10.45 kw at 95 per cent power factor and at the minimum speed of 4,400 revolutions per minute. It was designed for a field current which could be handled by the 28-volt 200-ampere voltage regulator normally available; therefore, it does not represent the lightest possible design which could be achieved by a heavier field current (12 amperes) and a special regulator. Its approximate weight is 33 pounds.

The stator is delta-connected with two parallel circuits per phase. There are 72 semienclosed slots with two slots per pole per phase and full pitch winding. Rectangular wire is used in the stator winding to obtain maximum ratio of copper to slot area. Paper insulation is used since the alternator temperature rise is kept within class *A* insulation limits. The 12-pole rotor is skewed. Metallic slot wedges are used for the rotational speeds encountered. The field coil ends are strapped to metallic reinforcing rings.

Forced draft cooling is employed. A six-inch total pressure differential is required with a two-inch blast pipe. The internal air passages are so arranged that reverse flow of cooling air is caused, and thus more efficient use is made of the available air.[9]

800-Ampere 30-Volt Electric System

The first unit (Figure 1) with rectifier having operating characteristics shown in Figure 8 and supplying a total output of 24.6 kw, 0.6 kw of which was used by the field circuit of the alternator, has been tested under ordinary laboratory conditions and has given an over-all efficiency of 72 per cent. The efficiencies of the transformer and rectifier are affected by the amount of cooling air and, therefore, by the operating temperature of the respective units, Figures 9 and 10. The amount of cooling air was varied from 750 cubic feet per minute to 1,300 cubic feet per minute. Over this range of volume of cooling air, the combined efficiency of the transformer rectifier unit remained practically constant, since the efficiency of the transformer decreases and that of the rectifier increases with the temperature rise. It follows then that the minimum volume of air can be chosen to maintain both components within their safe temperature limits.

High-Altitude Operations

The high-altitude operations constitute unusual conditions of rarefied air and extreme low temperature. The 200-ampere rectifier unit without transformer has been tested in a stratosphere chamber under 45,000 feet altitude and minus 43 degrees centigrade temperature conditions.

The results could readily be analyzed in the light of the same characteristics shown in Figures 9 and 10. Referring to Figure 9, it can be seen that rectifier efficiency decreases steadily as the temperature goes down. At the same time, however, efficiency increases with the reduction of the cooling air as a result of the higher internal temperature of the rectifier. The net result is that both effects tend to balance each other and to maintain the efficiency of the rectifier at a point governed by percentage of loading.

Conclusion

It is gratifying to comment here that at present a large amount of work is being carried on by capable engineers and manufacturers connected with or serving the aviation industry. The results no doubt will bring about the availability of several flexible electric systems from which to choose one meeting particular requirements.[9]

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Formulas for Calculating Short-Circuit Forces Between Conductors of Structural Shape*

THOMAS JAMES HIGGINS
ASSOCIATE AIEE

THE mechanical forces acting on bus-bar supports during short circuit are determined both by the magnitudes of the mutual electromagnetic forces exerted among the bus conductors and by the elastic properties of the supporting structure: its motional resistance, natural frequencies, and physical nature and arrangement. Comprehensive mathematical studies,** substantiated by experimental data, of the individual and collective effects of these factors and general methods for calculating the magnitudes involved have been advanced by Schurig and Sayre,[1] Dahlgren,[2,3] and Pilcher.[4] In shorter articles based on the work of the first-named Schurig, Fricke and Sayre,[5] Tanberg,[6] Specht,[7] Edson,[8] and others[9] have presented certain charts, nonograms, and short cuts that facilitate the numerical labor incident to actual calculation.

Now though the circumstances of a particular problem may render certain of these methods more advantageous to use than the others, *all require calculation of the mutual electromagnetic forces exerted among the conductors of the bus.* Commonly the bus is comprised of a group of long linear parallel nonmagnetic cylindrical conductors. If so, calculation of the electromagnetic force acting on a specified conductor resolves into successive determination of the magnitudes and phase relationships of the currents in the conductors, of the mutual force exerted between the given conductor and each of the others, and of appropriate vector summation of these last.

Calculation of the actual short-circuit currents requires use of symmetrical components. General procedures specifically adapted to short-circuit calculation of industrial-plant distribution systems, together with inclusive pertinent numerical data (electrical constants of standard types of conductor, of typical busses, and of the usual attached electrical apparatus), are encompassed in elaborate works by Hanna[10] and by Darling.[11,12] Illustratively, Schulze[13] has given a detailed numerical analysis of the short-circuit calculation of a typical plant distribution system. Other phases of the problem of calculation of the short-circuit currents have been discussed by Papst,[14] Schurig,[15] Leonard and Riker,[16] Gross,[17] Robinson,[18] Tripp,[19] and Killian.[20,21]

However, between finding the short-circuit currents (for which step are available all the methods and data contained in the papers mentioned in the preceding paragraph) and summing the forces exerted on the specified conductor by the other conductors (which step for a d-c bus requires but simple arithmetic and for an a-c bus the only slightly less simple task of adding complex numbers) is the intermediate and important task of *finding these individual forces.* Theoretically, if the conductor currents and geometry are known, these forces can be calculated by use of fundamental electromagnetic theory. Practically, the mathematical analysis requisite to use of this theory is far from simple—if not actually intractable—and the bus designer turns, perforce, to the use of known formulas or, if these latter be lacking, to some type of approximation, the accuracy of which is more or less indeterminate.

Obviously, resort to such approximation, certainly inconsistent with good engineering practice, is most undesirable. Yet there is little choice to the contrary. For, though the mechanical and electrical advantages stemming from the use of linear conductors of structural shape are so marked that, at present, most busses for heavy-current duty are constructed of such conductor; and, though standard designs of single and polyphase busses utilize strap, rectangular, tubular, channel, angle, tee, and I-beam conductor, a recent exhaustive survey[22] of the literature reveals, seemingly, that explicit formulas for calculating the electromagnetic forces between two conductors of the shapes mentioned are virtually nonexistent. A paper by Dwight[23] contains formulas and curves for calculating the force between two identical conductors having parallel coplanar axes and parallel-sided rectangular cross sections. And papers by Higgins[24,25] contain formulas for calculating the force between two identical conductors having parallel coplanar axes and parallel-sided full or hollow rectangular cross sections. But, despite extended and increasing use, there have not been published explicit formulas for calculating the force between conductors (not necessarily identical) of parallel-sided full or hollow rectangular conductors arranged other than just described; or for any arrangement of conductors of angle, channel, tee, or *I*-beam shape; or for any arrangement of composite conductors constructed of combinations of two or more of these six shapes.

Such formulas are derived in this paper, it being assumed that the conductors are nonmagnetic, are of such length that end effects are negligible, are right cornered, and carry current distributed uniformly over their cross sections.

Of these four assumptions, the first and second are commonly satisfied in practice. The third and fourth assumptions, though not always true in practice (for example, structural shapes built up of strap conductor are usually right cornered, whereas structural shapes drawn or rolled in one piece usually have slightly

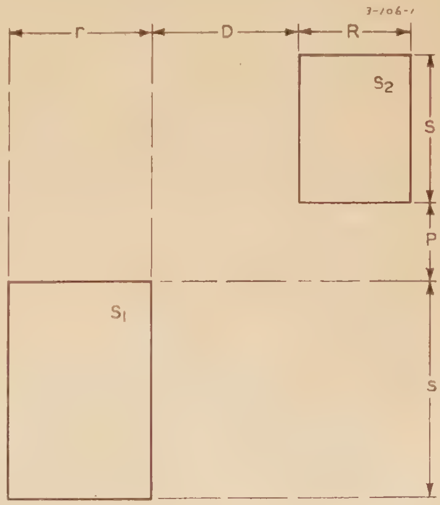

Figure 1

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THOMAS JAMES HIGGINS is associate professor of electrical engineering, Illinois Institute of Technology, Chicago, Ill.

* As the formulas pertinent to conductors of full or annular circular cross section are well known, they are not treated in this paper.

** While the work of the first named is frequently cited in the American literature, that of the other two, seemingly, has attracted but little attention. Yet Pilcher's work is an extension of the theory delineated by Schurig and Sayre; while Dahlgren's, an extended study of the important phenomenon of mechanical resonance, complements both.

rounded corners; again, skin and proximity effects are always present on a-c busses, though, as Beetz[26] has discussed at length, at power frequencies and normal bus spacings the effect of these phenomena on the magnitudes of the electromagnetic forces is vanishingly small) introduce error negligible with reference to the accuracy required in calculating short-circuit forces and enable solution of an otherwise intractable problem.

If these four postulates are granted, derivation of the desired formulas is not particularly difficult. First, through application of fundamental electromagnetic theory specific formulas are obtained for the components of the electromagnetic force exerted between two conductors, the cross sections of which are *arbitrarily located* parallel-sided rectangular areas. Then these formulas and the same electromagnetic principles are conjoined to obtain general formulas for the components of the force exerted between two conductors, the cross sections of which are arbitrary configurations comprised of parallel-sided rectangular areas (section I). Next, the theory essential to calculation of the derivatives of the geometric mean distances occurring in these formulas is developed (section II). Finally, as the general formulas of section I are applicable to an inclusive category that embraces conductors of structural shape, explicit formulas for the components of the force exerted between conductors of specified structural shape can be deduced from these general formulas as desired (section III).

I. General Formulas for the Components of the Electromagnetic Force

Invoking well-known theory, the inductance L of a circuit comprised of two conductors with rectangular cross sections S_1 and S_2 disposed as in Figure 1 is

$$L=2\log(D_{12}^2/D_{11}D_{22})$$

$$=-(2/I^2)\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\log D_{ij} \qquad (1)$$

wherein I is the magnitude of the circuit current; w_i and w_j are, respectively, the current densities in S_i and S_j, taken positive for current in one direction, negative for current in the other direction (whence w_i^2 and w_j^2 are positive, w_iw_j positive or negative depending on the relative direction of the two currents in S_i and S_j); and D_{ij} is the geometric mean distance of S_i from S_j.

The energy in the magnetic field asso-

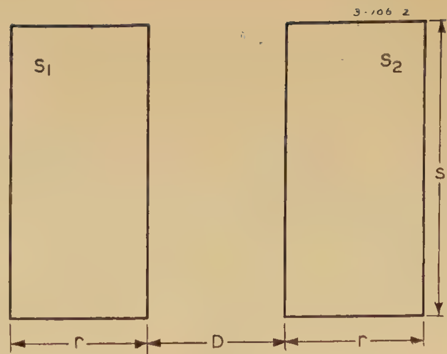

Figure 2

ciated with the circuit is $W=LI^2/2$; whence equation 1 yields

$$W=-\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\log D_{ij} \qquad (2)$$

As is well known, the rectangular components of the electromagnetic force exerted between the two conductors are given by

$$f_x\equiv f_D=\partial W/\partial D;\ f_y\equiv f_P=\partial W/\partial P \qquad (3)$$

Substituting from equation 2 in equation 3 as indicated yields

$$f_D=-\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\partial(\log D_{ij})/\partial D \qquad (4)$$

$$f_P=-\sum_{i=1}^{2}\sum_{j=1}^{2} w_iw_jS_iS_j\partial(\log D_{ij})/\partial P \qquad (5)$$

If each of the two conductors is comprised of several subconductors connected in parallel, one comprised of m subconductors numbered from 1 to m and the other of n subconductors numbered from $m+1$ to $m+n$, equation 23 of reference 27 yields for the inductance of the circuit

$$L=-(2/I^2)\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_iw_jS_iS_j\log D_{ij} \qquad (6)$$

As equation 6 is identical with equation 1 except for the difference in the upper limits of the summations, we obtain from equations 4 and 5 by analogy

$$f_D=-\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial D \qquad (7)$$

$$f_P=-\sum_{i=1}^{m+n}\sum_{j=1}^{m+n} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial P \qquad (8)$$

wherein f_D and f_P are the component forces on a conductor or a specified subconductor according as D and P are parameters locating one conductor with respect to the other or are parameters locating the subconductor with respect to the other $(m+n-1)$ subconductors.

If we have a double circuit comprised of $p+q$ conductors completely *in toto*, one circuit of two conductors being comprised of p subconductors numbered from 1 to p and the other of q subconductors numbered from $p+1$ to $p+q$, the two circuits carrying currents I_p and I_q respectively, equation 26 of reference 27 yields for the mutual inductance of the two circuits

$$M=-(1/I_pI_q)\left[\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j\log D_{ij}-\sum_{i=1}^{p}\sum_{j=1}^{p} w_iw_jS_iS_j\log D_{ij}-\sum_{i=p+1}^{p+q}\sum_{j=p+1}^{p+q} w_iw_jS_iS_j\log D_{ij}\right] \qquad (9)$$

As the second term within the braces is a function only of the parameters defining the geometry and relative location of the cross sections of the conductors comprising the circuit carrying I_p; as a similar statement is true for the third term; and as, therefore, both terms are independent of the parameters, say D' and P', used to locate the conductors of one circuit relative to the other, differentiation of the right-hand member of equation 9 yields

$$f_{D'}=-\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j\,\partial(D_{ij})/\partial D' \qquad (10)$$

$$f_{P'}=-\sum_{i=1}^{p+q}\sum_{j=1}^{p+q} w_iw_jS_iS_j\,\partial(D_{ij})/\partial P' \qquad (11)$$

wherein $f_{D'}$ and $f_{P'}$ are the component forces exerted on a bus, a conductor of the bus, or a subconductor of the bus, according as P' and D' are parameters used to locate the bus, a conductor of the bus, or a subconductor of the bus from the like elements of the other bus. If, then, to the component force on a specified conductor or subconductor obtained from equations 10 or 11 we add the corresponding value obtained from equations 7 or 8, which value is the force exerted on the conductor or subconductor by the other like elements of the *same* circuit, we obtain the total force on the specified conductor or subconductor.

In an n-phase system comprised of n distinct conductors, one conductor, say a, can be considered as carrying current I_a (the currents are expressed as complex

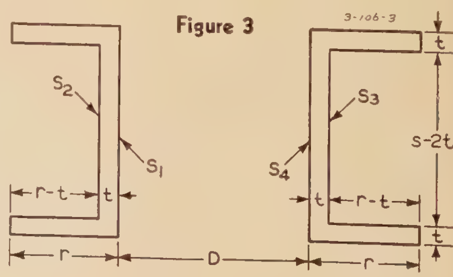

Figure 3

numbers) and the remaining $(n-1)$ conductors can be considered as connected in parallel and carrying the return current. Accordingly, our problem is interpretable as a single-phase circuit comprised of two conductors, one full, the other divided, whence we have from equations 7 and 8

$$f_D=-I_a\sum_{i=b}^{n} I_i\,\partial(\log D_{ai})/\partial D \quad (12)$$

$$f_P=-I_a\sum_{i=b}^{n} I_i\,\partial(\log D_{ai})/\partial P \quad (13)$$

wherein P and D are parameters locating S_a with respect to $S_b, \ldots, S_n$.

If the individual conductors are divided, a composed of a' subconductors numbered from 1 to a', b of b' subconductors numbered from $a'+1$ to $a'+b'$, and correspondingly for the others, we have from equations 7 and 8

$$f_D=-\sum_{i=1}^{a'}\sum_{j=1}^{a'+\ldots+n'} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial D \quad (14)$$

$$f_P=-\sum_{i=1}^{a'}\sum_{j=1}^{a'+\ldots+n'} w_iw_jS_iS_j\,\partial(\log D_{ij})/\partial P \quad (15)$$

where now f_D and f_P give the component forces on the entire conductor a or on any subconductor of a, say a', according as D and P are parameters locating conductor a with respect to the other $n-1$ conductors, or are parameters locating subconductor a' with respect to the other $a'+\ldots+n'-1$ subconductors.

With regard to the physical quantities mentioned to this point, all units are in the absolute system: linear dimensions in centimeters, current I in abamperes; self inductance L and mutual inductance M in abhenrys per centimeter of bus length; energy W in ergs per centimeter of bus length; force f in dynes per centimeter of bus length.

Reviewing the analysis of this section, we see that in each case treated the total force, $f=(f_D^2+f_P^2)^{1/2}$, is determined in both magnitude and direction providing the derivatives indicated in the summations are expressible as known functions of the dimensions of the conductors and bus. Accordingly, we turn now to development of formulas for calculating these derivatives.

II. Calculation of the Derivatives of the Geometric Mean Distances

In earlier papers the writer[27,28] has derived the geometric mean distance D_{12} of a rectangular area S_1 from a second arbitrarily located parallel-sided rectangular area S_2, dimensioned as in Figure 1, to be

$$S_1S_2\log D_{12}=-(25/12)S_1S_2-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}K(A_i,B_j) \quad (16)$$

wherein

$$K(A_i,B_j)=(A_i^4-6A_i^2B_j^2+B_j^4)\times\log(A_i^2+B_j^2)+4A_iB_j(B_j^2-A_i^2)\times\tan^{-1}(B_j/A_i)-2\pi A_iB_j \quad (17)$$

and

$$A_1=|D+R+r|;\ A_2=|D+R|;\ A_3=|D|;\ A_4=|D+r| \quad (18)$$

$$B_1=|P+S+s|;\ B_2=|P+S|;\ B_3=|P|;\ B_4=|P+s| \quad (19)$$

The parameter D in equation 18, and similarly P in equation 19, may be positive, zero, or negative. Thus, if $r=R$, $s=S$, $D=-r$, and $P=-s$, the two areas S_1 and S_2 are identical and superimposed, and equation 16 yields the formula for the self geometric mean distance of a rectangular area. Other more detailed illustrations of application of equation 16 to the determination of the geometric mean distance between two variously located rectangular areas are contained in section V of reference 27 and in section III of this paper.

Turning to calculation of the derivatives indicated in the summations of the preceding section, we have from equation 16 that

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\times\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}\partial K(A_i,B_j)/\partial A_i dA_i/dD \quad (20)$$

From equation 17

$$\partial K(A_i,B_j)/\partial A_i=4A_i(A_i^2-3B_j^2)\times\log(A_i^2+B_j^2)+4B_j^2(B_j^2-3A_i^2)\times\tan^{-1}(B_j/A_i)+A_i^3-3A_iB_j^2-2\pi B_j^3 \quad (21)$$

From equation 18

$$dA_i/dD=d|D+\text{constant}|/dD=\pm1 \quad (22)$$

the plus or the minus sign to be chosen according as in a particular problem the specified value of D is such that the quantity within the bars is positive or is negative. Finally, substituting equations 21 and 22 in equation 20 yields the desired formula

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\times\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}[4A_i(A_i^2-3B_j^2)\times\log(A_i^2+B_j^2)^{1/2}+4B_j(B_j^2-3A_i^2)\times\tan^{-1}(B_j/A_j)+A_i^3-3A_iB_j^2-2\pi B_j^3]dA_i/dD \quad (23)$$

Certain variants of equation 23 are of value. If dA_i/dD is of the same sign for $i=1$ to 4, then

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}A_i^3dA_i/dD=2\pi\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}B_j^3dA_i/dD=0 \quad (24)$$

whence equation 23 reduces to

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\times\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}[4A_i(A_i^2-3B_j^2)\times\log(A_i^2+B_j^2)^{1/2}+4B_j(B_j^2-3A_i^2)\times\tan^{-1}(B_j/A_i)-3A_iB_j^2]dA_i/dD \quad (25)$$

Further, if $dA_i/dD=1$ for $i=1$ to 4, then

$$3\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+}A_iB_j^2dA_i/dD=0 \quad (26)$$

whence, in turn, equation 25 reduces to

$$S_1S_2\,\partial(\log D_{12})/\partial D=-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)\quad U(A_i,B_j) \quad (27)$$

wherein, for convenience of future reference,

$$U(A_i,B_j)=4A_i(A_i^2-3B_j^2)\log(A_i^2+B_j^2)^{1/2}+4B_j(Bj^2-3A_i^2)\tan^{-1}(B_j/A_i) \quad (28)$$

By virtue of the identity, $K(A_i,B_j)=K(B_j,A_i)$ (to be obtained from equation 17 by interchange of A_i with B_j and subsequent use of the trigonometric identity, $\tan^{-1}(A_i/B_j)+\tan^{-1}(B_j/A_i)=\pi/2$), corresponding expressions for $S_1S_2\partial\times(\log D_{12})/\partial P$ follow from those for $S_1S_2\partial(\log D_{12})/\partial D$ through interchange of A_i and B_j and replacement of D by P. As this operation is easily effected, for economy of space we here set forth only the analog of equation 27:

$$S_1S_2\,\partial(\log D_{12})/\partial P=-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}(VA_i,B_j) \quad (29)$$

wherein

$$V(A_i,B_j)=4B_j(B_j^2-3A_i^2)\log(A_i^2+B_j^2)^{1/2}+4A_i(A_i^2-3B_j^2)\tan^{-1}(A_i/B_j) \quad (30)$$

Equations 16 to 30 inclusive suffice for calculation of the derivatives occurring in the double summations defining f_D and f_P. Accordingly, inasmuch as for any given set of data the components f_D and f_P are completely determinable through use of the analysis developed to this point, it follows that this analysis comprises the general solution of the problem of calculating the electromagnetic force, $f=(f_D^2+f_P^2)^{1/2}$, exerted on a specified conductor or subconductor of a d-c single-phase or polyphase bus comprised of conductors the cross sections of which are arbitrary configurations of parallel-sided rectangular areas.

In general the double summation indicated in the right-hand member of each of equations 23, 25, and 27 is calculated by inserting the numerical values of the parameters A_i and Bj (determined from equations 8 and 9 respectively), obtaining the indicated logarithms and arc tangents from suitable tables, and performing the necessary arithmetic. If, however, it happens—and this case is the rule rather than the exception—that the bus and conductor geometry is such that equation 27 is valid and, further, that therein the summation to be effected is of a form typified by

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i, B_j) = \sum_{j=1}^{4}(-1)^{j}F(D, r, B_j) \quad (31)$$

whereof, typically,

$$F(D, r, s) = 2U(D, s) - U(D+r, s) - U(D-r, s) \quad (32)$$

the U functions being those defined by equation 28, the numerical computation can be effected more quickly and with less chance of numerical error through use of the rapidly converging Taylor's series in which the right-hand member of equation 32, considered as a function of D, can be expanded about the point $D=r$; that is, by

$$F(D, r, s) = 12r^2[2s \tan^{-1}(s/D) - D \times \log(D^2+s^2) + (20/12) - Dr^2/6(D_2+s^2) - Dr^4(D^2-3s^2)/90(D^2+s^2)^3 - Dr^6(D^4-10D^2s^2+5s^4)/420(D^2+s^2)^5 - \ldots] \quad (33)$$

The details of this expansion are given in the appendix. Substituting appropriately from equation 33 in equation 31, noting that in the resulting expansion

$$\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}(20/12) = 0$$

simplifying, and then substituting in equation 27 we have

$$S_1S_2\, \partial(\log D_{12})/\partial D = (-1/2)\sum_{j=1}^{4}(-1)^{j}G(D, r, B_j) \quad (34)$$

wherein

$$G(D, r, B_j) = Dr^2[2n \tan^{-1} n - \log(D^2+B_j^2) - m^2/6(1+n^2) - m^4(1-3n^2)/90(1+n^2)^3 - m^6(1-10m^2n^2+5n^4)/420(1+n^2)^5 - \ldots] \quad (35)$$

and $m=r/D$, $n=B_j/D$.

Analogously, if equation 27 holds, we have from equation 28

$$S_1S_2\, \partial(\log D_{12})/\partial P = -(1/2)\sum_{i=1}^{4}(-1)^{i}H(P, A_i, s) \quad (36)$$

wherein

$$H(P, A_i, s) = Ps^2[2m \tan^{-1} m - \log(P^2+A_i)^2 - n^2/6(1+m^2) - n^4(1-3m^2)/90(1+m^2)^3 - n^6(1-10n^2m^2+5m^4)/420(1+m^2)^5 - \ldots] \quad (37)$$

and now $m=A_i/P$, $n=s/P$.

Again, it often happens in practice that the right-hand member of equation 34 reduces to, typically,

$$\sum_{j=1}^{4}(-1)^{j}G(D, r, B_j) = -2G(D, r, s) + 2G(D, r, 0) \quad (38)$$

If so, by virtue of equations 35 and 38, we have from equation 34 the very convenient form

$$S_1S_2\, \partial(\log D_{12})/\partial D = DM(D, r, s) \quad (39)$$

where $M(D, r, s)$ is defined by

$$M(D, r, s) = r^2[2n \tan^{-1} n - \log(1+n^2) + m^2n^2/6(1+n^2) + \ldots] \quad (40)$$

and $m=r/D$, $n=s/D$.

Analogously, from equation 37 we have

$$S_1S_2\, \partial(\log D_{12})/\partial P = PN(P, r, s) \quad (41)$$

where $N(P, r, s)$ is defined by

$$N(P, r, s) = s^2[2m \tan^{-1} m - \log(1+m^2) + n^2m^2/6(1+m^2) + \ldots] \quad (42)$$

and now $m=r/P$, $n=s/P$.

III. Some Illustrative Examples

Example 1 is advanced as corroborative of the preceding analysis. Example 2 is advanced as demonstrative of application of this analysis to calculation of the electromagnetic force exerted between two conductors of structural shape.

Example 1

A single-phase bus is comprised of two identical strap conductors (Figure 2). To calculate the electromagnetic force per unit length:

For this case: $r=R$; $s=S$; $D=D$; $P=-s$; $S_1=S_2=rs$; $w_1=-w_2=I/S_1=I/rs$. Then equations 18, 19, and 22 yield $A_1=D+2r$; $A_2=A=D+r$; $A_3=D$; $B_1=B_3=S$; $B_2=B_4=0$; $\partial A_i/\partial D=1$, $i=1, 2, 3, 4$. From equation 4, noting that the self geometric mean distances, D_{11} and D_{22}, are independent of the parameter D,

$$f_D = -\sum_{i=1}^{2}\sum_{j=1}^{2}w_iw_jS_iS_j\, \partial(\log D_{ij})/\partial D = -2w_1w_2S_1S_2\, \partial(\log D_{12})/\partial D = (2I^2/r^2s^2)S_1S_2\, \partial(\log D_{12})/\partial D \quad (43)$$

By virtue of equations 27, 31, 32, 34, and 39 we obtain

$$S_1S_2\, \partial(\log D_{12})/\partial D = -(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i, B_j)$$
$$= -(1/24)\sum_{j=1}^{4}(-1)^{j}[2U(D+r, B_j) - U(D+2r, B_j) - U(D, B_j)]$$
$$= -(1/24)\sum_{j=1}^{4}(-1)^{j}F(D+r, r, B_j)$$
$$= -(1/2)\sum_{j=1}^{4}(-1)^{j}G(D+r, r, B_j)$$
$$= G(D+r, r, s) - G(D+r, r, 0)$$
$$= (D+r)M(D+r, r, s) \quad (44)$$

wherein

$$M(D+r, r, s) = r^2[2n \tan^{-1} n - \log(1+n^2) + m^2n^2/6(1+n^2) + \ldots] \quad (45)$$

and $m=r/(D+r)$, $n=s/(D+r)$.

Finally, substituting from equation 44 in equation 43 we have

$$F_D = [2I^2/(D+r)][(D+r)^2M(D+r,r,s)/r^2s^2]$$ dynes per centimeter of bus length (46)

or in units more commonly used: current I in amperes; D, r, s, in inches

$$f_D = [5.4I^2/(D+r)][(D+r)^2 \times M(D+r, r, s)/r^2s^2]10^{-7}$$ pounds per foot of bus length (47)

The dimensionless term in the braces, commonly represented by the symbol k_D, is termed the *electromagnetic space factor* for f_D.

By symmetry the component $f_p=0$. Accordingly, the total force, $f=f_D$, is given by equation 46 or 47. Allowing for the difference of notation, these formulas for f_D agree with those obtained earlier by H. B. Dwight[23] (who first considered this problem) and by the writer.[24]

Example 2

A single-phase bus is comprised of two identical channels placed back to back (Figure 3). To calculate the electromagnetic force per unit length of bus:

Each of the two channels can be considered as comprised of two component conductors: one of full rectangular cross section wherein current exists in the same direction as in the channel, the other of smaller rectangular cross section having current in the opposite direction. Hence $S_1=S_4=rs$; $S_2=S_3=(r-t)(s-2t)$; $w_1=-w_2=w_3=-w_4=I/(S_1-S_2)=I/t(s-2r-2t)$.

From equation 7, noting that the self geometric mean distances, $D_{11}=D_{44}$ and $D_{22}=D_{33}$, are independent of the parameter D, we obtain

$$f_D = -\sum_{i=1}^{4}\sum_{j=1}^{4}w_iw_jS_iS_j\, \partial(\log D_{ij})/\partial D = [2I^2/t^2(s-2r-2t)^2][S_1^2\, \partial(\log D_{14})/\partial D + S_2^2\, \partial(\log D_{23})/\partial D - 2S_1S_2\, \partial(\log D_{13})/\partial D] \quad (48)$$

From equations 18, 19, and 22 we obtain for the three sets of (A_i, B_j) parameters

S_1, S_4: $A_1=D+2r$; $A_2=D+r$; $A_3=D$
$B_1=B_3=s$; $B_2=B_4=0$

S_2, S_3: $A_1=D+2r$; $A_2=A_4=D+r+t$; $A_3=D+2t$
$B_1=B_3=s-2t$; $B_2=B_4=0$

S_1, S_3: $A_1=D+2r$; $A_2=D+r+t$; $A_3=D+t$; $A_4=D+r$
$B_1=B_3=s-t$; $B_2=B_4=t$

The values of the first two derivatives in the right-hand member of equation 48 can be written down by analogy with the value found for the derivative in equation 43. Thus

$$S_1^2\,\partial(\log D_{14})/\partial D=(D+r)M(D+r,\,r,\,s) \tag{49}$$

and

$$S_2^2\,\partial(\log D_{23})/\partial D = (D+r+t)M(D+r+t,\,r-t,\,s-2t) \tag{50}$$

Calculation of the third derivative is effected in precisely the same fashion as in equation 44:

$$S_1S_2\,\partial(\log D_{13})/\partial D$$
$$=-(1/24)\sum_{i=1}^{4}\sum_{j=1}^{4}(-1)^{i+j}U(A_i,\,B_j)$$
$$=-(1/24)\sum_{j=1}^{4}(-1)^j([-U(D+2r,\,B_j)+U(D+r+t,\,B_j)-U(D+t,\,B_j)+U(D+r,\,B_j)]$$
$$=-(1/24)\sum_{j=1}^{4}(-1)^j[-U(D+2r,\,B_j)-U(D+t,\,B_j)+2U(D+r+2^{-1}t,\,B_j)+U(D+r+t,\,B_j)+U(D+R,\,B_j)-2U(D+r+2^{-1}t,\,B_j)]$$
$$=-(1/2)\sum_{j=1}^{4}(-1)^j[G(D+r+2^{-1}t,\,r-2^{-1}t,\,B_j)-G(D+r+2^{-1}t,\,t/2,\,B_j)]$$
$$=(D+r+2^{-1}t)[M(D+r+2^{-1}t,\;r-2^{-1}t,s-t)-M(D+r+2^{-1}t,2^{-1}t,s-t)+M(D+r+2^{-1}t,r-2^{-1}t,t)+M(D+r+2^{-1}t,\,2^{-1}t,\,t)] \tag{51}$$

Finally, substitution of equations 49, 50 and 51 in equation 48 yields the desired formula

$$f_D=[2I^2/t^2(s-2r-2t)^2]\{(D+r)M(D+r,r,s)+(D+r+t)\,M(D+r+t,\,r-t,\,s-2t)-(D+r+2^{-1}t)[M(D+r+2^{-1}t,\,r-2^{-1}t,\,s-t)-M(D+r+2^{-1}t,\,2^{-1}t,\,s-t)+M(D+r+2^{-1}t,\,r-2^{-1}t,\,t)+M(D+r+2^{-1}t,\,2^{-1}t,\,t)]\}\text{ dynes per centimeter of bus length} \tag{52}$$

wherein, typically, $M(D+r,\,r,\,s)$ is given by equation 45.

As by symmetry $f_P=0$, we have $f=f_D$. Inasmuch as the term, electromagnetic space factor, has significance—at least in the sense hitherto employed—only if each of the cross sections of the two conductors possess two principal axes of symmetry, k_D is not to be distinguished. Accordingly, equation 52 encompasses the complete solution of the problem.

Appendix

Consider as a function of D alone

$$F(D,\,r,\,s)=2U(D,\,s)-U(D+r,\,s)-U(D-r,\,s)$$

can be expanded in a Taylor's series about the point $D=r$. Accordingly, by Taylor's theorem we have

$$F(D,\,r,\,s)=-r^2\partial^2U(D,\,s)/\partial D^2-(r^4/2\cdot 3!)\partial^4U(D,\,s)/\partial D^4-(r^6/3\cdot 5!)\partial^6U(D,\,s)/\partial D^6-\ldots$$

wherein

$$U(D,\,s)=4D(D^2-3s^2)\log(D^2+s^2)^{1/2}+D(D^2-3s^2)\tan^{-1}(s/D)$$
$$\partial U(D,\,s)/\partial D=6(D^2-s^2)\log(D^2+s^2)-24Ds\tan^{-1}(s/D)+4(D^2-s^2)$$
$$\partial^2U(D,\,s)/\partial D^2=12D\log(D^2+s^2)-24s\tan^{-1}(s/D)+20D$$
$$\partial^3U(D,\,s)/\partial D^3=12\log(D^2+s^2)-44$$
$$\partial^4U(D,\,s)/\partial D^4=24D(D^2+s^2)^{-1}$$
$$\partial^5U(D,\,s)/\partial D^5=-24(D^2-s^2)(D^2+s^2)^{-2}$$
$$\partial^6U(D,\,s)/\partial D^6=48D(D^2-3s^2)(D^2+s^2)^{-3}$$
$$\partial^7U(D,\,s)/\partial D^7=-144(D^4-6D^2s^2+s^4)\times(D^2+s^2)^{-4}$$
$$\partial^8U(D,\,s)/\partial D^8=576D(D^4-10D^2s^2+5s^4)\times(D^2+s^2)^{-5}$$

Substitution of the derivatives in the preceding series yields

$$F(D,\,r,\,s)=12r^2[2s\tan^{-1}(s/D)-D\times\log(D^2+s^2)+(20/12)-Dr^2/6(D^2+s^2)-Dr^4(D^2-3s^2)/90(D^2+s^2)^3-Dr^6(D^4-10D^2s^2+5s^4)/420(D^2+s^2)^5-\ldots]$$

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Distribution Factors and Pitch Factors of the Harmonics of a Fractional-Slot Winding

M. M. LIWSCHITZ
MEMBER AIEE

Synopsis: The investigation of the fractional-slot windings with respect to their harmonic (differential) leakage, to noise, and so forth, requires the knowledge of the distribution factors of their harmonics.

Very valuable work on distribution and pitch factors of fractional-slot windings has been done by J. F. Calvert.[1] On the basis of charts he worked out distribution-factor tables covering the actual harmonics up to the 24th. Once the chart of the winding has been set up, a simple formula can be used for computing the distribution factors of the harmonics for any three-phase fractional-slot winding.

In the following paper, simple formulas are derived for the distribution factors of the harmonics of fractional-slot windings which make the layout of charts unnecessary. These formulas are similar to those of the integral-slot windings. In deriving them the usual method of attack, namely, the use of charts, had to be abandoned and the vector diagram of the different slots used instead.

The Actual Harmonic of a Fractional-Slot Winding

IT has been shown in a previous paper[2] that

$$\left(\frac{n'}{p}+1\right)\frac{\beta}{2}=K \quad K \text{ integer excluding 0} \quad \text{(1a)}$$

and

$$\left(\frac{n'}{p}\mp 1\right)\frac{\beta}{2m_1}=K_1 \quad K_1 \text{ integer including 0} \quad \text{(1b)}$$

are the criteria for the existence of the n'th harmonic in the magnetomotive force of a fractional-slot winding. (For symbols see nomenclature.)

When the minus sign is used in the equation 1b, the harmonic travels with the rotation; when the plus sign is used, the harmonic travels opposite to the rotation.

Since n' has to satisfy both equations 1a and b, the actual harmonics produced by the winding will be different for different values of β and p. We have to distinguish between the case when β is equal to an even number and the case when β is equal to an odd number. It can be found easily that the actual harmonics of the three-phase windings are as follows:

When β = even number

$$n'=\frac{2p}{\beta}\nu \quad \text{where } \nu=1, 2, 3, 4, 5\ldots \quad (2)$$

For the synchronous (main) wave $n'=p$ and, therefore, $\nu_s=\beta/2$. The number of subharmonics is equal to $(\beta-2)/2$.

When β = odd number

$$n'=\frac{p}{\beta}\nu \quad \text{where } \nu=1, 3, 5, 7\ldots \quad (3)$$

For the synchronous wave here $\nu_s=\beta$ and the number of subharmonics is $(\beta-1)/2$.

The harmonics which are a multiple of three do not exist in the three-phase machine (except when zero-sequence currents flow in the winding).

The Slot Star

The use of the slot star instead of the generally used chart not only facilitates the layout of the fractional-slot winding but also makes it possible to derive simple formulas for the distribution factor and pitch factor of the harmonics of this winding.

Figure 1 represents the slot star of a winding with an integral number of slots per pole per phase, namely $q=2$. The integral-slot windings can be considered as a special case of the fractional-slot winding having $\beta=1$. Thus each pole represents a repeatable group (a unit) and only one pole has to be considered. The angle between two slots is

$$\alpha_s=\frac{180}{3\times 2}=30°$$

To two adjacent vectors correspond two adjacent slots. The vector 7, with which the next pole starts, is shifted 180 degrees with respect to the vector 1. As regards the synchronous (main) wave, it is taken care of by a proper connection between the coil groups.

We consider now a winding with $1^1/_2$ slots per pole per phase, $q=1^1/_2$. If we write in general

$$q=\frac{N}{\beta} \quad (4)$$

β poles represent a repeatable group (a unit), each phase has N slots in β poles, and the total number of slots in β poles is equal to $3N$.

In our example two poles make a unit, each phase has three slots per unit, and the total number of slots per unit is equal to nine. The angle between two slots is as before.

$$\alpha_s=\frac{180}{mq} \quad (5)$$

and it is equal to $180/4.5=40$ degrees. The angles which correspond to the nine slots of the unit are

Slot	1	2	3	4	5	6	7	8	9	10
Angle	0	40	80	120	160	200	240	280	320	360 = 0°

To slot 10 corresponds the angle zero degrees; slot 10 is the beginning of the next repeatable group. Slots 1 to 5 lie under the first pole, slots 6 to 9 lie under the second pole of the unit. Since the connections between the coil groups take into account a shifting of 180 degrees, the real angles between the slots, that is, the shifting of the slots with respect to each other in the magnetic field, are

Slot	1	2	3	4	5	6	7	8	9	10
Angle	0	40	80	120	160	20	60	100	140	180°

and the slot star of the repeatable group is given by Figure 2. Between vectors 1 and 2, which correspond to slots 1 and 2, lies the vector which corresponds to slot 6; between vectors 2 and 3, which correspond to slots 2 and 3, lies the vector which corresponds to slot 7, and so on. The winding creeps in the magnetic field. In general, between two vectors which correspond to two adjacent slots lie $(\beta-1)$ other vectors. We have to distinguish between the angle between

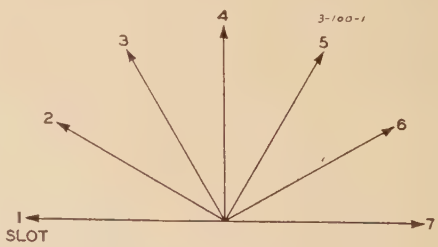

Figure 1. Slot star of a winding with $q=2$

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M. M. LIWSCHITZ is with The Polytechnic Institute of Brooklyn, Brooklyn, N. Y., and is also consulting engineer for Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

two slots as given by equation 5 and the angle between two adjacent vectors. This latter angle is the magnetic-field angle between the slots of the repeatable group; this angle determines the behavior of the winding with respect to its magnetomotive force and electromotive force, that is, its distribution factors and pitch factors.

The magnetic-field angle is

$$\alpha_m=\frac{180}{Nm} \tag{6}$$

$\alpha_s=\alpha_m$ only for the winding with an integral number of slots per pole per phase, because for this winding $\beta=1$ and $N=q$. For the fractional-slot windings α_m is always smaller than α_s. It is

$$\frac{\alpha_s}{\alpha_m}=\beta \tag{7}$$

Apparently, the highest distribution factor for the synchronous wave will be obtained when the first three vectors of the slot star, Figure 2, are assigned to phase I, the following three vectors are assigned to phase II, and the last three vectors to phase III. Thus, in each repeatable group phase I will occupy slots 1, 2, and 6; phase II, slots 3, 7, and 8; and phase III, slots 4, 5, and 9.

We consider the sequence of the slots in the slot star, Figure 2. If we start with slot 1, the sequence of the slots follows the series

$1, 1+5, 1+2\times5, 1+3\times5 \ldots\ldots$

Since the total number of slots in the repeatable group is equal to $3\times N$, this value (or a multiple of it) has to be subtracted from the terms of the series when they are larger than $3N$.

In general, the series is

$1, 1+d, 1+2d, 1+3d \ldots\ldots$

where d is the difference between two slots which correspond to two adjacent vectors of the slot star.

This difference d can be found from the following consideration.[3] If we denote by P the number of full pole pitches between two slots which correspond to two adjacent vectors of the slot star (in our example $P=1$), then there will be

$$d\times\alpha_s=\alpha_m+180P$$

Inserting α_s and α_m from equations 5 and 6 there will be

$$d=\frac{m_1NP+1}{\beta} \tag{8}$$

For P the smallest integer for which d becomes an integer has to be inserted. P is equal to or larger than one.

The Distribution Factors of Three-Phase Windings

The slot star Figure 2 relates to the synchronous wave, the length of which is equal to 2τ. With respect to this wave the winding behaves as if there are N slots per pole per phase shifted with respect to each other by the magnetic-field angle α_m, which is equal to the angle between two adjacent vectors. Thus, the distribution factor of the synchronous wave is

$$K_d(n'=p)=\frac{\sin N\alpha_m/2}{N\sin\alpha_m/2}=\frac{\sin 30°}{N\sin 30°/N} \tag{9}$$

In order to determine the distribution factor of the n'th harmonic, it is only necessary to determine the angle between two adjacent vectors of Figure 2 for this harmonic. The number of slots per pole per phase N is the same for all harmonics.

We consider first the wave with the length equal to $2\beta\tau$. With respect to this wave the angle between two slots is,

$$\alpha_{s\beta}=\frac{\alpha_s}{\beta}$$

Since the winding behaves with respect to this wave as a normal integral-slot winding the magnetic-field angle must be equal to the slot angle, that is,

$$\alpha_{m\beta}=\alpha_{s\beta}=\frac{\alpha_s}{\beta}=\alpha_m$$

Figure 3 shows the slot star with respect to this wave. Adjacent vectors lie in adjacent slots. To two adjacent vectors of Figure 2 corresponds here the angle $d\alpha_m$. For example, to the two adjacent vectors 1 and 6 of Figure 2 corresponds here the angle $5\times\alpha_m=d\alpha_m$. Thus, for the harmonic the wave length of which is equal to $2\beta\tau$ the angle between two adjacent vectors of Figure 2 is equal to $d\alpha_m$. It is necessary to add to this angle 180 degrees when P is an odd number, in order to take into account the opposite direction of the current.

When we consider now the fundamental wave ($n'=1$), the wave length of which is equal to $2p\tau$, the angle between two adjacent vectors of Figure 2 for this wave will be $d\alpha_m\beta/p$ or $d\alpha_m\beta/p+180$ when P is an odd number. Therefore, the corresponding angles for the n'th harmonics are

$$\alpha_{mn'}=n'd\alpha_m\frac{\beta}{p} \quad \text{(when } P \text{ is an even number)} \tag{10}$$

$$\alpha_{mn'}=n'd\alpha_m\frac{\beta}{p}+180 \quad \text{(when } P \text{ is an odd number and } \beta \text{ is even)}$$

$$\alpha_{mn'}=n'(d\alpha_m+180)\frac{\beta}{p} \quad \text{(when } P \text{ is an odd number and } \beta \text{ is odd)}$$

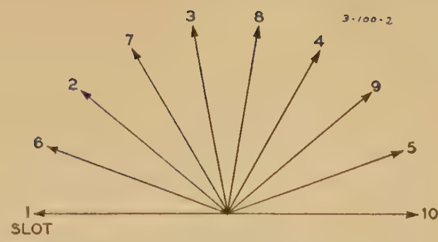

Figure 2. Slot star of a winding with $q=1^1/_2$ with respect to the synchronous wave

and the distribution factor of the n'th harmonic is

$$K_{dn'}=\frac{\sin N\,\alpha_{mn'}/2}{N\sin\alpha_{mn'}/2} \tag{11}$$

It can be shown that the numerator of equation 11 is equal to sin 30 degrees for all harmonics which are not a multiple of 3. We apply equations 2 and 3 to equation 10. We find:

When β = even number

$$n'=\frac{2p}{\beta}\nu \quad \nu=1, 2, 4, 5, 7 \ldots\ldots \nu_s=\frac{\beta}{2}$$

and

$$\alpha_{mn'}=2d\alpha_m\nu+180 \tag{12}$$

P is here always an odd number: since N is here an odd number, d can become an integer only when P is an odd number.

When β = odd number

$$n'=\frac{p}{\beta}\nu \quad \nu=1, 5, 7, 11, 13 \ldots\ldots \nu_s=\beta$$

$$\alpha_{mn'}=d\alpha_m\nu \quad \text{(when } P \text{ is an even number)}$$

$$\alpha_{mn'}=d\alpha_m\nu+180 \quad \text{(when } P \text{ is an odd number)} \tag{13}$$

When β is an even number, N and P are odd numbers, d can be an odd as well as an even number, but d is not divisible by three. Thus, with N = odd number and

$$\alpha_m=\frac{180}{3N}$$

$$\sin N\,\alpha_{mn'}/2=\sin N(d\alpha_m+90)=\cos d\nu\times60°$$

The product $d\nu$ is not divisible by three, therefore

$$\sin N\alpha_{mn'}/2=\cos d\nu\times60=\cos 60°=\sin 30°$$

When β is an odd number, N, P, and d can be even as well as odd numbers; but d is not divisible by three; further, d is an odd number when N or P is an even number, and d is an even number when N and P are odd numbers. We have to consider the cases where P is even and those where P is odd.

When P is an even number, then

$$\sin N\alpha_{n'}/2=\sin Nd\alpha_m/2\times\nu=\sin d\nu\times30$$

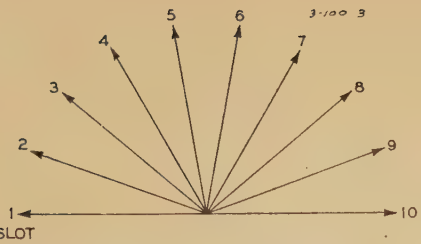

Figure 3. Slot star of a winding with $q=1^1/_2$ with respect to the wave whose length is equal to $2\beta\tau$

d and ν are odd numbers, not divisible by three. Thus $d\nu$ is an odd number not divisible by three and

$$\sin N\alpha_{mn'}/2 = \sin d\nu \times 30 = \sin 30 \text{ degrees}$$

When P is an odd number and N is an even number, then

$$\sin N\alpha_{mn'}/2 = \sin N(d\alpha_m/2\times\nu+90) = \sin Nd\alpha_m/2\times\nu = \sin d\nu\times 30$$

and the result is the same as for P equal to an even number.

When P is an odd number and N is an odd number, then d is an even number, and there will be

$$\sin N\alpha_{mn'}/2 = \sin N(d\alpha_m/2\times\nu+90) = \sin(d\nu\times 30+90) = \cos d\nu\times 30$$

Since $d\nu$ is an even number

$$\sin N\alpha_{mn'}/2 = \cos d\nu\times 30 = \sin 30°$$

Therefore, we can write for equation 11

$$K_{dn'} = \frac{\sin 30°}{N\sin \alpha_{n}'/2} \tag{14}$$

$\alpha_{n'}$ is given by equations 12 and 13. Inserting these equations and also equations 6 and 8, we find finally

When β = even number

$$K_{dn'} = \frac{0.5}{N\cos\left(\frac{d}{N}60\nu\right)} \tag{15}$$

When β = odd number

$$K_{dn'} = \frac{0.5}{N\sin\left(\frac{d}{N}30\nu\right)} \text{ (when } P \text{ is an even number)} \tag{16}$$

$$K_{dn'} = \frac{0.5}{N\cos\left(\frac{d}{N}30\nu\right)} \text{ (when } P \text{ is an odd number)} \tag{17}$$

Equations 15 to 17 are valid for the absolute values only. Equations 10 and 11 yield also the right signs.

Examples

Three-phase machine with 14 poles and $q=11/7$. Thus, $N=11$, $\beta=7$. Equation 8 yields $d=19$ with $P=4$. Since β is an odd number and P an even number, equation 16 has to be applied. It is $p=\beta$ and, therefore, $n'=\nu$ (see equation 3).

It is valid

$$K_{dn'} = \frac{0.5}{11\sin\frac{19}{11}30} = 0.058 \text{ for the fundamental wave}$$

$$K_{dn'} = \frac{0.5}{11\sin\frac{19}{11}30\times 5} = 0.0463 \text{ for the fifth harmonic}$$

and so forth.

Eight-pole machine with $q=11/4$, $N=11$, $\beta=4$, $d=25$ with $P=3$.

For $2p/\beta=2$, $n'=2\nu$ (see equation 2). Thus,

$$K_{dn'} = \frac{0.5}{11\cos\frac{25}{11}60} = 0.0625 \text{ for the second harmonic}$$

$$K_{dn'} = \frac{0.5}{11\cos\frac{25}{11}60\times 4} = 0.0457 \text{ for the eighth harmonic}$$

and so forth.

The Pitch Factors of Three-Phase Windings

For the pitch factor of the synchronous wave

$$K_{p(n'=p)} = \sin\frac{W}{\tau}90 \tag{18}$$

Denoting by Z the coil width in slot pitches, we can write for the synchronous wave

$$K_{p(n'=p)} = \sin\frac{Z}{mq}90 = \sin\frac{Z\beta}{mq\beta}90$$

$$= \sin\frac{Z\beta}{mN}90 = \sin Z\beta\frac{\alpha_m}{2} \tag{18a}$$

Referring α_m as in section 3 to the wave the length of which is equal to the total circumference of the armature ($2p\tau$), we find for the pitch factor of the n'th harmonic

$$K_{pn'} = \sin Z\beta\frac{\alpha_{n'}}{2} \tag{19}$$

$\alpha_{n'}$ is given by equations 12 and 13. As for the distribution factor, it must be distinguished between the case when β is an even number and the case when β is an odd number. Inserting in equation 19 equations 12, 8, and 6 for β = even number, and equations 13, 8, and 6 for β = odd number, we find in agreement with Calvert

$$K_{pn'} = \sin\frac{W}{\tau}\frac{180}{\beta}\nu \quad \text{(when } \beta = \text{even number)} \tag{20}$$

$$K_{pn'} = \sin\frac{W}{\tau}\frac{180}{2\beta}\nu \quad \text{(when } \beta = \text{odd number)} \tag{21}$$

Nomenclature

d—difference between two slots which correspond to two adjacent vectors of the slot star.

$K_{dn'}$—distribution factor of the n'th harmonic.

$K_{pn'}$—pitch factor of the n'th harmonic.

m_1—number of phases.

n'—order of the harmonic with respect to a fundamental whose wave length is equal to the circumference of the armature.

N—numerator of the fraction that fixes the number of slots per pole per phase.

p—number of pole pairs.

P—number of full pole pitches between two slots which correspond to two adjacent vectors of the slot star.

$q=\frac{N}{\beta}$—number of slots per pole per phase.

W—coil width.

Z—coil width in slot pitches.

$\alpha_s=\frac{180}{mq}$—angle between two slots.

$\alpha_m=\frac{180}{mN}$—angle between two vectors of the slot star.

β—denominator of the fraction that fixes the number of slots per pole per phase.

τ—pole pitch (in the units of W).

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TRANSACTIONS SECTION

Preprint of Corresponding Pages From the Current Annual AIEE Transactions Volume

Any discussion of these papers will appear in the December 1943 Supplement to Electrical Engineering—Transactions Section

Operation of Nonsalient-Pole-Type Generators Supplying a Rectifier Load

M. D. ROSS
MEMBER AIEE

J. W. BATCHELOR
ASSOCIATE AIEE

It has been recognized that the presence of harmonic currents in the supply circuit are a source of additional heating in the generators supplying a rectifier load, particularly where a six-phase rectifier is the total load. Robert Pohl has presented papers on this subject, and from his work has concluded that an appreciable reduction in the rating of turbine generators is required when the rectifiers are connected for 6-phase and 12-phase operation.[1,2] Experience in this country with turbine generators operating with rectifier loads indicated that the reduction factors presented by Doctor Pohl were too large, so a test program was carried out to determine the practical limits of such loads. The test results and experience both indicate that for 12-phase operation and above, the effect on the supply system is very small and in most cases may be neglected.

Any harmonic current flowing in the stator winding of a generator increases the temperature rise of the stator winding and surrounding parts. Also, these harmonics establish a magnetomotive force which results in currents flowing in the surface of the rotor, adding to the rotor heating. It can be shown that some harmonics rotate backward, and some harmonics rotate forward with respect to the fundamental.[3] Because it rotates against the rotation of the rotor, the magnetomotive force set up by the fifth harmonic results in a sixth harmonic on the surface of the rotor. Since the rotation of the seventh harmonic is in the same direction as that of the rotor, it also results in a sixth harmonic on the rotor surface. These two magnetomotive forces, rotating in opposite directions, result in a pulsating field on some axis of the rotor. Likewise, another pulsating field of 12 times normal frequency is set up by the action of the 11th and 13th harmonics. The harmonics, up to the 25th, present in the stator and rotor are shown in Table I.

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M. D. Ross is section engineer and J. W. Batchelor is design engineer, both in the a-c generator engineering department of Westinghouse Electric and Manufacturing Company, East Pitstburgh, Pa.

The authors acknowledge the assistance of J. H. Cox and L. A. Kilgore.

Tests

The temperature tests were made on a 1,250-kva 2,300-volt three-phase, 60-cycle 3,600-rpm turbine generator supplying a 600-volt ignitron rectifier connected for double three-phase operation, as shown in Figure 2. Double three-phase is a form of six-phase operation. The regulation of the rectifier was approximately ten per cent. That is, the impedance of the lines, transformer, and rectifier was of such a value that there was a drop in voltage equal to ten per cent of the normal voltage when full generator load was applied. In addition, the lines were not supplied from what might be considered an infinite system, but instead, from a single generator. The subtransient reactance of this generator was approximately 11 per cent. The importance of this fact and its effect on the magnitude of harmonic currents will be shown later. The generator was operated at approximately full kilovolt-amperes for four temperature runs, with the temperature of the stator winding being measured by detector, and the temperature of the rotor winding being measured by the increase in resistance. Oscillograms of the wave form were taken during each run, in order to obtain the magnitude of the harmonic currents to be used in subsequent calculations. After each test, the rotor was inspected to determine whether burning had occurred, either between the wedges and the teeth, or at the joints between the retaining rings and the rotor body. There was, however, no evidence of burning on any of the runs, indicating that excessive heating did not occur under the loading conditions of the test. Table II shows the results of the tests. In order to obtain the additional temperature rise of the stator and rotor windings, preliminary temperature runs were made at zero load and full voltage; full kilovolt-amperes and 90 per cent power factor; and full kilovolt-amperes and zero per cent power factor. Using the results of these tests, it was possible to determine what the temperatures would have been if the harmonic currents had not been included in the load current. The additional temperature rise shown in the tables is the difference between the measured temperature rise and the normal temperature rise for that particular load.

Rotor Heating

In order to calculate the heating of the rotor, the loss can be determined by the use of the negative-sequence resistance of

Table I. Harmonics Present in Stator and Rotor

Order of Harmonic	Stator Harmonics: 6-Phase Connection	Stator Harmonics: 12-Phase Connection	Resultant Rotor Harmonics: 6-Phase Connection	Resultant Rotor Harmonics: 12-Phase Connection	Rotation of Harmonics
1	1	1			Forward
3					
5	5		6		Backward
7	7		6		Forward
9					
11	11	11	12	12	Backward
13	13	13	12	12	Forward
15					
17	17		18		Backward
19	19		18		Forward
21					
23	23	23	24	24	Backward
25	25	25	24	24	Forward

Table IIA. Generator Test Results—Temperature Runs

Rectifier Connected for Double Three-Phase Operation

Phase Delay (Degrees)	Voltage	Current	Power Factor	Stator Temperature Rise (Degrees Centigrade) Total	Stator Temperature Rise (Degrees Centigrade) Additional	Rotor Temperature Rise (Degrees Centigrade) Total	Rotor Temperature Rise (Degrees Centigrade) Additional	Ambient Temperature (Degrees Centigrade)
0	2300	297	90.	40.	2.6	79.1	19.1	28.8
14.1	2300	302	89.3	40.8	2.8	86.	22.	29.7
24.7	2300	296	83.5	42.5	5.1	93.1	22.1	25.4
30.4	2300	295	81.5	44.	6.6	95.5	25.5	28.8

Table IIB. Generator Test Results—Per Cent Harmonic Currents in Supply Circuit

Rectifier Connected for Double Three-Phase Operation

Order of Harmonic	Phase Delay (Degrees) 0	14.1	24.7	30.4
1	99.0	99.5	98.26	98.3
3	0.10	1.76	0.99	1 80
5	11.80	13.50	15.30	15.30
7	6.72	7.24	5.60	6.63
9	0.11	1.29	0 48	2 05
11	2.41	4.10	5.77	5.38
13	1.75	2 92	3 05	3.89
17	1.03	1.10	2 94	2.61
19	1.18	0.53	1.59	2.28
23	0.33	1.17	1 59	1.15
25	0.76	0.89	0.86	1.02

the rotor. A description of the test method for obtaining this value is given in the appendix. A minor correction in the value of resistance must be made, since the loss varies as the 1.8 power of the negative-sequence current for turbine generators.[4] Since the fifth and seventh harmonics result in two sixth-harmonic waves rotating in opposite directions on the surface of the rotor, the current to be used in calculating the sixth-harmonic loss can be taken as the sum of the fifth and seventh harmonics. This figure of loss must be multiplied by three, for the three phases, and by two, because at 120 cycles only one-half the rotor loss is $I_2^2R_2$, the other half being supplied through the shaft. The sum of the harmonic currents gives the maximum loss for the complete periphery of the rotor, but the actual loss is an average between the points of maximum and minimum loss. Thus, the maximum loss must be reduced by a factor, K_a, in order to obtain the average loss. The factor K_a may be obtained from Figure 5, once the ratio of the two harmonic currents is known. The derivation of the curve in Figure 5 is given in the appendix.

In obtaining the temperature rise, a factor, K_v, of 0.11 watt per degree centigrade per square inch of rotor surface, neglecting the surface of the retaining rings, was used for this particular peripheral speed. An air velocity of one-half the peripheral speed of the rotor was assumed. Since the temperature rise was measured on the rotor winding instead of the rotor surface, it was necessary to take into account the portion of the rotor winding which was affected by the additional loss on the rotor surface. The construction of the rotor for this machine was such that the ventilation of the portion of the coils beneath the retaining rings was very good, and it was assumed that all the extra heat in these sections of the winding was dissipated to the air passing over the end turns of the rotor winding, leaving only the slot portion of the winding to be affected by the additional loss on the surface of the rotor. On this rotor, the slot portion of the winding was 54 per cent of the mean turn, and this value was used for the winding ventilation factor, K_w. This factor may vary widely, depending on the physical proportions of the rotor, as well as the amount of ventilation on the end turns of the rotor winding.

The temperature rise of the rotor winding caused by the harmonic currents may be calculated for the various even-numbered harmonics from the following equation, using the currents of order $n-1$ and $n+1$, where n is the order of the harmonic on the surface of the rotor.

$$\text{Temperature rise} = 2\times3\times\frac{K_w}{K_v\times S}\times \Sigma K_a(I_{n-1}+I_{n+1})^2(R_2-R_1)\sqrt{\frac{f}{120}}$$

where

K_a = average loss factor
K_w = winding ventilation factor
K_v = heat-dissipation factor
S = rotor surface ($\pi\times$rotor diameter$\times$ core length)

Table III compares the calculated and test values for the four temperature runs.

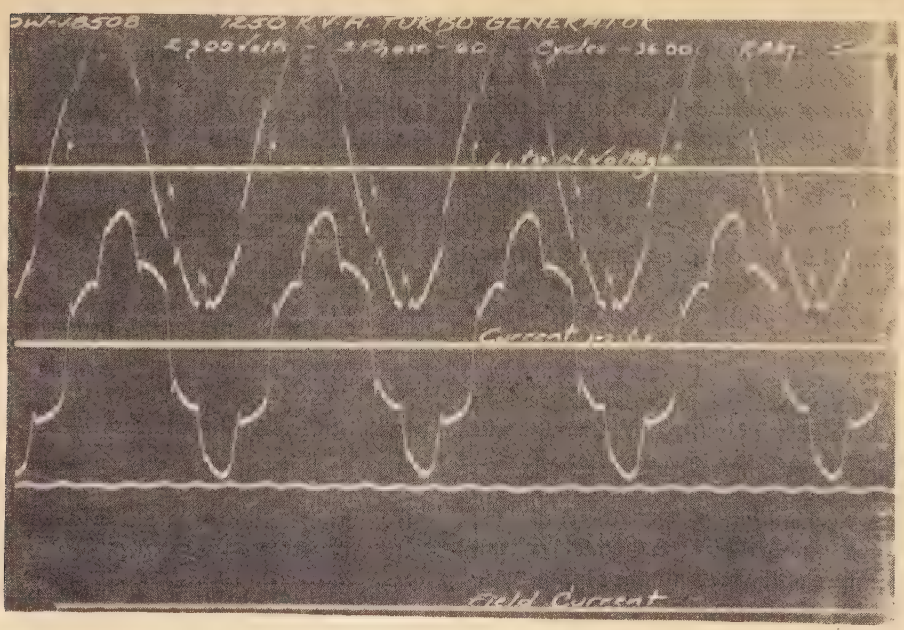


Figure 1. Oscillogram showing wave form in generator field and armature circuits

Double three-phase ignitron operating with 30.4 degrees phase delay. 1,250-kva turbo-generator, 2,300 volts, three phase, 60 cycles, 3,600 rpm

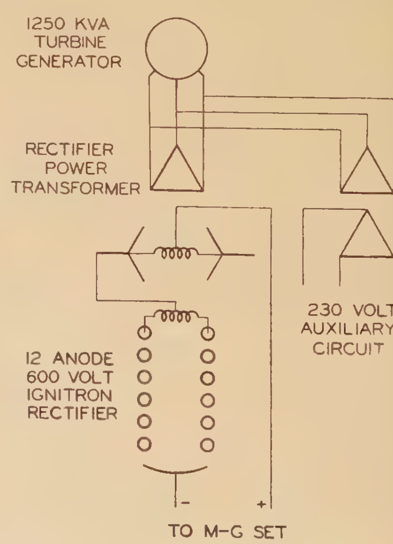


Figure 2. Schematic diagram of test circuit

Table III. Additional Temperature Rise of Rotor Winding

Reduction Factors Applying to This Machine Connected to Double Three-Phase Ignitron

Phase Delay (Degrees)	Additional Rise (Degrees Centigrade)		Load Reduction Factor	
	Test	Calculated	Test	Calculated
0	19.1	19.3	1.0	1.0
14.1	22.	27.6	0.995	0.95
24.7	22.1	32.3	0.92	0.85
30.4	25.5	32.2	0.9	0.85

The reduction in load to maintain not more than 85 degrees centigrade rotor temperature rise was calculated for each load condition and is given in Table III as a reduction factor. To determine this value, the additional temperature rise of the rotor caused by the harmonic currents was varied as the square of the load current, and the rise caused by the field current was calculated from the saturation curves of the machine. The two components were added by trial until the proper figure was obtained. It should be noted that these reduction factors apply only to this particular machine, and they cannot be generalized to cover all machines.

Figure 3 (above). 1,250-kva turbine generator on test

Stator Heating

The calculation of the heating of the stator winding is complicated further by the fact that the temperatures by detector are affected not only by the I^2R loss as determined by the eddy factor, but also by the heating of the stator parts, and by the extra loss on the surface of the rotor. It has been suggested that the load on the stator should be reduced according to the square root of the ratio of the normal loss to the loss with rectifier load as calculated by the use of the eddy factor alone, and neglecting any effect of core loss or rotor loss.[1] This method has been followed in the calculations for Table IV. These calculations have been made for the average loss, using the average eddy factor and for the maximum loss in the top strand. The effective eddy factor, $K_{\text{eff.}}$, equals $\Sigma I^2K_e/\Sigma I^2$, and the reduction, R, in load equals $\sqrt{K_e}$ (fundamental)/$K_{\text{eff.}}$ The reduction factors, R and R', for the average and top strands, respectively, may be compared with the reduction factors from the measured additional temperature rise by inspection of Table IV.

The stator temperature rise on the normal full-load test was 38 degrees centigrade, which is considerably below the maximum value of 60 degrees centigrade rise. While this stator had considerable temperature margin, it was thought worth while to prepare a series of load reduction factors based on maintaining the full-load temperature rise of 38 degrees centigrade with various rectifier loads. It was assumed that the permissible load varied as the square root of the ratio of the temperature rise under normal load to the temperature rise with rectifier load. The reduction factors for this machine alone are given in Table IV.

Table IV. Additional Temperature Rise of Stator Winding

Reduction Factors Applying to This Machine Connected to Double Three-Phase Ignitron

Phase Delay (Degrees)	Additional Rise From Test (Degrees Centigrade)	Reduction Factor From Test	Calculated Reduction Factor	
			Average	Top Strand
0	2.6	0.966	0.963	0.95
14.1	2 8	0.965	0.94	0.917
24.7	5.1	0.937	0.915	0.895
30.4	6.6	0.922	0.907	0.887

12-Phase Operation

The foregoing tests and calculations were all on the basis of the rectifier operating with a double three-phase connection. If the connection had been 12 phase, the magnitude of the 5th, 7th, 17th, and 19th harmonics would have been reduced, with some improvement in power factor. In order to compare the performance with double 3-phase and 12-phase connections, the calculations have been made, eliminating the 5th,

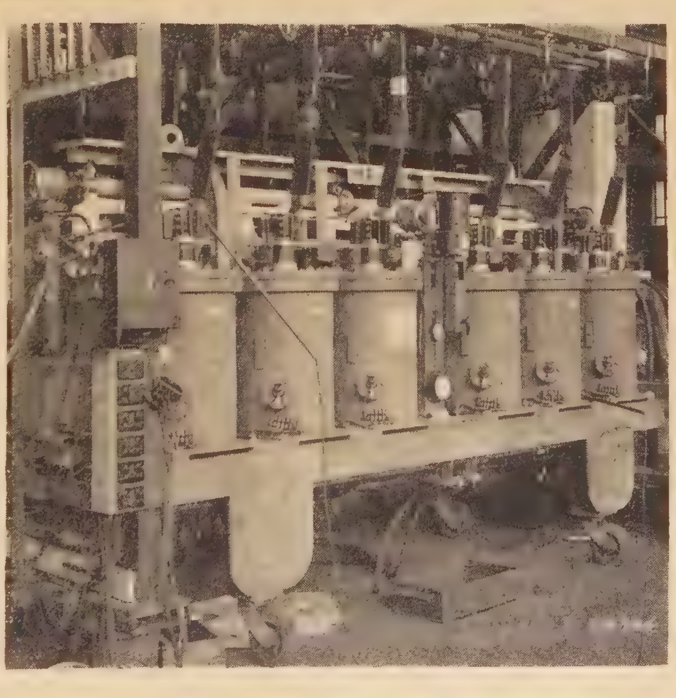


Figure 4 (right). 12-anode ignitron on test

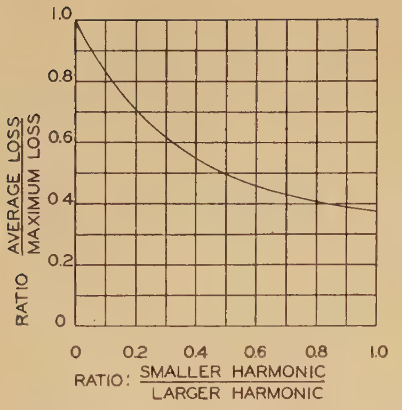


Figure 5. Values of rotor loss factor K_a

7th, 17th, and 19th harmonics, and assuming the magnitudes of the others were unchanged. The calculated reduction factor for the stator winding was 0.96 compared with 0.907 for the condition of 30.4 degrees phase delay. The calculated additional temperature rise for the rotor winding was approximately six degrees for the 30.4-degree delay condition compared to 25.5 degrees for the double three-phase connection Considering the improvement in power factor and the lower temperature rise, the rotor heating for the assumed conditions will not affect the permissible loading.

Conclusions

From these tests and calculations, and from previous studies of rectifier operation, the following conclusions may be drawn:

1. A rectifier load is the source of harmonic currents in the supply system. These harmonic currents cause extra heating in the stator and rotor of a generator, and, in some cases, may require a reduction in rating of supplying generators.

2. The order of the harmonic currents is affected by the number of phases of rectifier operation, and their magnitude is affected by the voltage reduction or phase delay, and the impedance of the complete system.[5,6,7] The lowest-order harmonic current which exists in the supply circuit is $P-1$, where P is the number of phases of rectifier operation. With a rectifier operating with a 12-phase connection, the additional heating of the generator will be slight, and with a number of phases higher than 12, the additional heating will be negligible. The magnitude of the harmonic currents is reduced with a reduction in degrees ignitor delay and an increase in system impedance.

3. For a given load, the rotor heating is more pronounced than the stator heating, but operation at a relatively high power factor will introduce some margin in the rotor heating. Under certain conditions of operation the stator heating becomes the limiting factor.

4. It is not possible to state any definite rule for the reduction in rectifier load necessary in order to keep generator temperatures within recognized limits, because the extra heating is affected by the physical proportions of the machine, as well as the loading conditions. It is possible, however, to determine the magnitude of the harmonic currents,[5,6], once the loading conditions are known. The extra heating and any necessary reduction in load can be determined by the generator designer, using the methods outlined in this discussion.

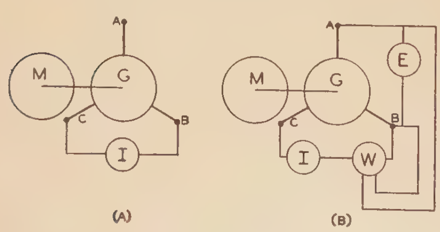


Figure 6. Generator connections for obtaining negative-sequence resistance

Appendix A

Method of Obtaining Negative-Sequence Resistance

The negative-sequence resistance may be determined by either one of two methods.[4,8]

For Figure 6a, drive the generator at synchronous speed.

$$R_2=\frac{3(P-P_{F\&W})}{2I^2}$$

where

P = shaft input in watts
$P_{F\&W}$ = friction and windage loss in watts
I = phase current

For Figure 6b, drive the generator at synchronous speed.

$$\text{Power factor}=\frac{P}{E\times I}=\cos\theta$$

$$Z_2=3\frac{E}{I}$$

$$R_2=\frac{E}{3}\frac{\sin}{I}\theta$$

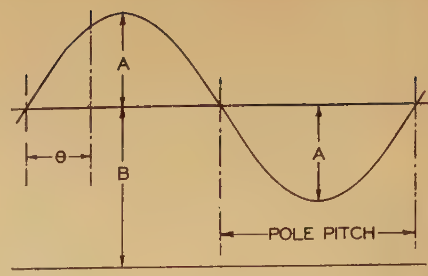
where

P = power in watts
E = line-to-line voltage
I = phase current

It should be noted that both of these methods require a sustained, single-phase short circuit, and care should be taken to see that the rotor is not overheated.

Appendix B

Since the fifth harmonic current rotates against the direction of rotation of the rotor, it generates a sixth harmonic current in the rotor surface. The seventh harmonic current rotates with the rotor, also generating a sixth harmonic current in the rotor surface, but the direction of rotation is opposite to that caused by the fifth harmonic current. Thus, there are two points of maximum loss and two points of minimum loss, with a sinusoidal distribution between. This relationship holds for any pair of harmonic currents which generate any particular frequency in the rotor surface. The distribution is shown in Figure 7, where A is the smaller of the two harmonic currents and B is the larger.



"A" IS THE SMALLER OF TWO HARMONIC CURRENTS

Figure 7. Resultant current distribution around rotor periphery

Let average loss around periphery equal L and angle around periphery equal θ, then

$$L=\frac{1}{2\pi}\int_0^{2\pi}(B+A\sin\theta)^2d\theta$$

$$=\frac{1}{2\pi}\left[B^2\theta-2AB\cos\theta+A^2\left(\frac{\theta}{2}-\frac{\sin 2\theta}{4}\right)\right]_0^{2\pi}$$

$$=B^2+\frac{A^2}{2}$$

Figure 5 gives the ratio of the average loss to the maximum loss for any ratio of the two harmonic currents.

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Rotating Regulator for Arc Furnaces

R. A. GEISELMAN
NONMEMBER AIEE

C. C. LEVY
ASSOCIATE AIEE

W. R. HARRIS
ASSOCIATE AIEE

Synopsis: Increased production of steel alloy for the war effort has brought about a wider use of electric-arc furnaces, and with this improvements have been made in automatic regulation of electrode position.

The Rototrol regulator described in this paper is of the rotating type which controls electrode position as determined by response to current in the electrode and voltage between the electrode and the furnace shell. Features and performance of a typical installation are illustrated.

AUTOMATIC adjustment of electrode position has always been used as a means of controlling the power input to steel-melting arc furnaces. This is necessary because the charge melts and flows away from the electrode so that the electrode must be lowered to maintain the arc. The scrap being melted frequently falls against the electrode, making withdrawal necessary to remove the short circuit. Furthermore, adjustment is necessary to allow for natural electrode consumption.

In addition to this the arc is a pure resistance load, and changing its length by adjustment of the electrode position gives a simple and satisfactory method of controlling the current.

The early arc-furnace regulator was a simple current-operated device. The electrode current in each phase was supplied from a current transformer to the coil of the regulator element. The pull of this coil was then balanced against a spring, and the operation of the regulator contacts caused the hoist motor to run in either direction to raise or lower the electrode as the current varied from some predetermined value. Figure 1 shows a typical arc furnace for steel melting. The hoist motors as shown are connected through a wire cable and drum sheave so as to lower and raise the electrode masts from which the electrodes are supported. This mechanical system is varied somewhat with different designs of furnace, but the electrical problem is simply one of automatic control, suitable for reversing and braking the electrode motor.

Current regulation was generally unsatisfactory when precise control was necessary, chiefly because the restraining force of the spring had a constant value. The variations of the current in the circuit are rapid at times, and the mechanical movement of the electrode is slow. Therefore, the position of the electrode responded not to instantaneous but to an average value of current variation. Furthermore, a three-phase system of currents in an electric furnace is complex. The furnace charge is a ground which connects through three continually shifting phase arcs to the potential supplied by the transformer. It is clear that the currents in the regulator elements are not independent but rather are interdependent; the value in any phase at a given time is determined by conditions in the other two phases, as well as by the length of the arc in that particular phase.

As is well known, the voltage drop across the arc will increase as the current is decreased and vice versa. In practice this characteristic can be modified by inserting additional reactance in the circuit. In fact, a sufficient value of reactance must be present in the circuit to maintain stability, so that the combination of arc resistance and circuit impedance will have a positive impedance characteristic over the operating zone. These theoretical requirements of the arc-furnace circuit have been frequently discussed in other articles on the subject and are well known. It should not be forgotten, however, that a considerable part of the circuit reactance in the case of the larger furnaces is in the secondary leads, and, therefore, such furnaces require no additional reactance for stable operation.

It is readily understood that, when the varying pull of the coil or other regulating device which is proportional to current in the electrode, is matched against a restraining force that is proportional to the voltage drop across the arc, the utmost sensitivity to changes in arc length is obtained in the regulator. While this sensitivity may not be so important when the furnace is reducing the cold scrap to a molten bath, it is of great importance when this molten bath is being refined.

The development of the art over a period of years showed that this principle of regulation was correct. It was natural that further development should look for improvements of the controlling scheme for the motor which for many years had been a constant-potential d-c, reversing control combined with dynamic braking.

Quite independently of the arc-furnace industry, however, new methods of regulation had been introduced in other industrial applications where regulated control of motors to adjust for speed, tension, rate of acceleration, and so forth, were desired. These methods for the

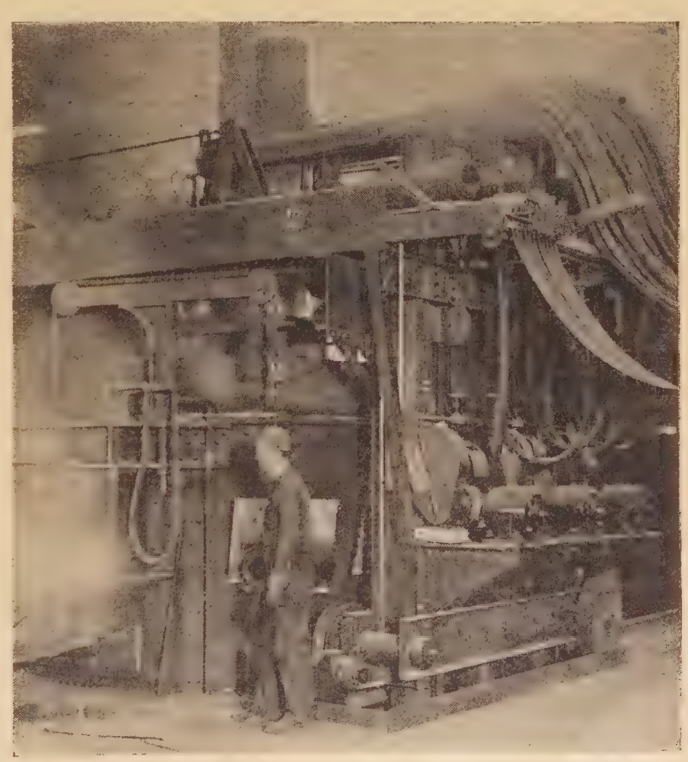

Figure 1. Representative arc furnace for steel melting

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R. A. Geiselman, C. C. Levy, and W. R. Harris are all with Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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Figure 2. Typical unit including Rototrol, a-c motor, and d-c generator

most part were based on a different means of operating the motor in response to the regulating element. Instead of using a constant-potential control, adjustable-voltage control of the motors was used and has been found very satisfactory. An independent source of d-c voltage is required and is usually supplied by a motor generator set. If the exciter for this generator is supplied with suitable control fields, a complete regulating system is formed in which the regulating element is a part of the power supply for the electrode motors.

The Rototrol regulator was first applied to an automatic welding head in 1928. A few years later it was applied to the large elevators in Radio City and, since then, has found wide use in many industrial applications. It is the heart of the adjustable-voltage planer drive and has been proved in service over a period of several years. Other applications include paper-mill drives, electric shovels, mine sweepers, skip hoists, bomb spinners, and machine tools, some of which provide speed ranges as high as 120 to 1.

The rotating-type arc-furnace regulator was thus a natural development prompted by the use of similar regulators in other applications. It has been favorably received chiefly because the direct tie between the generator and the motor eliminates all contactors in the motor circuits.

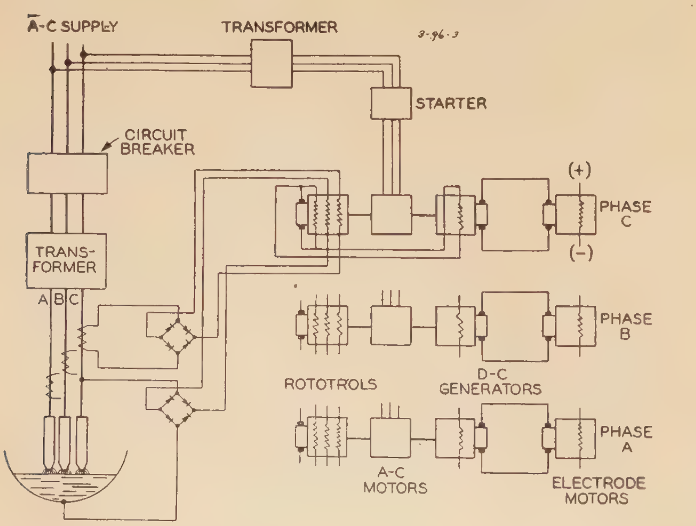
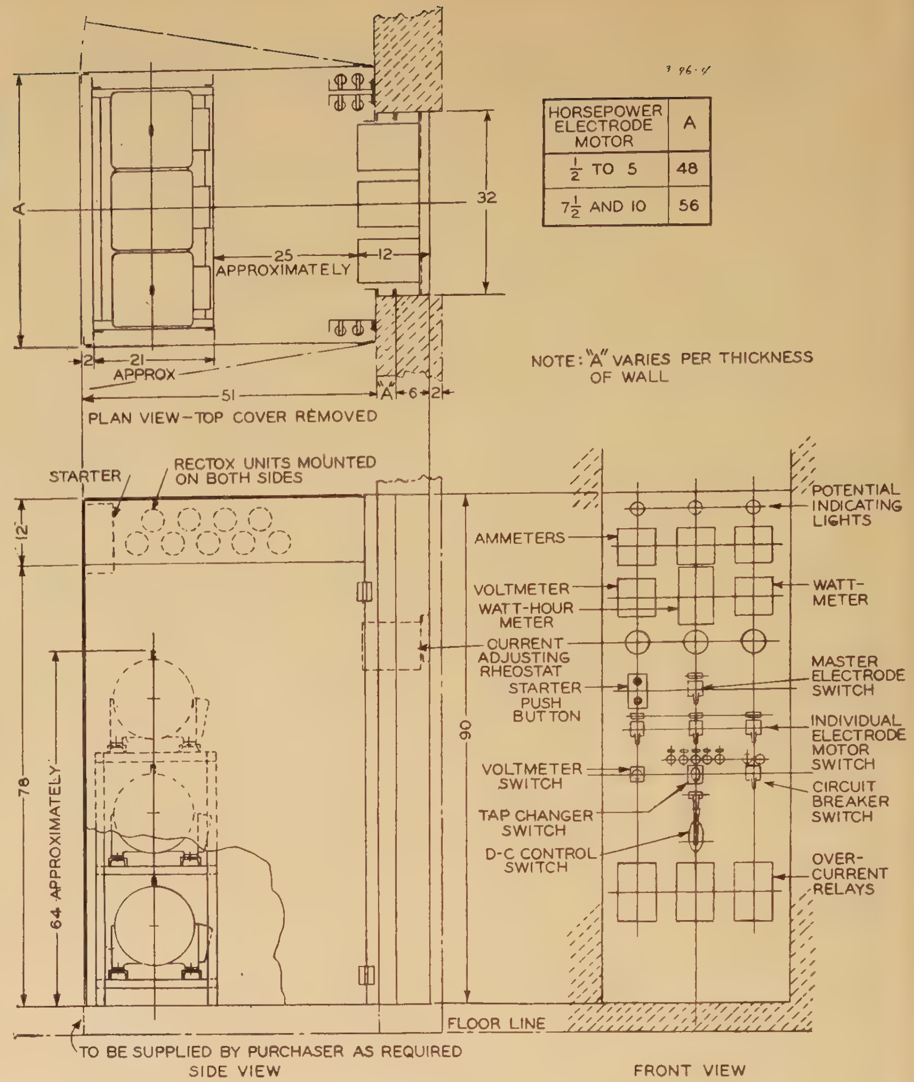


Figure 3. Elementary diagram of arc-furnace regulating system



HORSEPOWER ELECTRODE MOTOR	A
$\frac{1}{2}$ TO 5	48
$7\frac{1}{2}$ AND 10	56

Figure 4. Metal-enclosed Rototrol arc-furnace control and regulator unit

The Rototrol is essentially a small d-c machine similar in both construction and theory of operation to the standard d-c generator of like size. It is driven by a standard squirrel-cage induction motor as part of the motor generator set, Figure 2. The magnetic circuit is excited by a number of field windings, and the machine functions entirely through the interaction of these fields. The field coils constitute the input of the device, while the output is obtained from the armature circuit. The principal field windings include:

1. A control field which serves as a reference against which the quantity to be regulated is compared.

2. A control field which measures the quantity to be regulated.

3. A self-energizing field connected either in series or in shunt with the armature.

For a high degree of accuracy, the various control fields should have complete control over the regulating element without supplying any of the magnetomotive force necessary to generate power in its own output circuit. The self-energizing field circuit is designed so that its resistance line will be coincident with the air-gap line of the no-load saturation curve. This results in a high degree of amplification since an extremely small change of power in the control fields is ample to change the output over its full range.

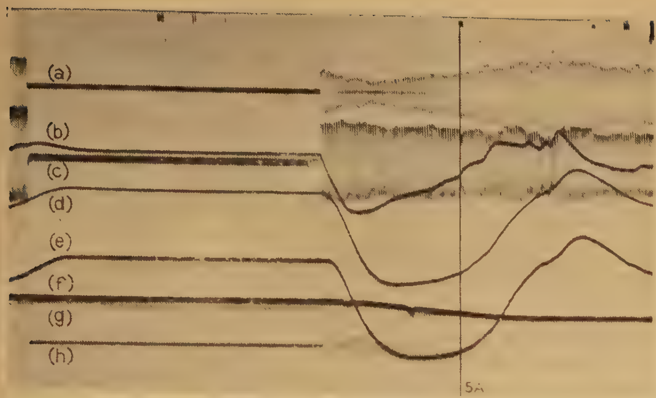


Figure 5. Oscillogram showing electrical performance of regulator during the melt-down period

(a). Electrode current to Rototrol control field
(b). Rototrol armature volts
(c). A-c volts to Rototrol control field
(d). Motor armature volts
(e). Motor speed
(f). Electrode position indicator
(g). Motor armature current
(h). Electrode current

This output is applied to the field of the constant-speed direct-connected generator, which supplies power to the electrode motors.

The speed of response of the system comprising the arc-furnace regulating element and the power-supplying generator must be carefully co-ordinated with the acceleration characteristics of the electrode motor in order to obtain the maximum rate of acceleration permissible without exceeding the commutating ability of the electrode motor. Acceleration of the electrode motor is influenced by the system inertia, the friction load, and the torque developed by the motor. For instance, a three-horsepower equipment with a system WR^2 at the motor shaft of 1.5 pound-feet squared, a friction load of two-thirds full-load torque and a maximum motor torque of 1.5 times rated will accelerate to rated speed in about one half of a second.

The speed of response of the regulating element depends on the ratio of inductance to resistance for each field, the mutual induction between the fields, and the relative ratios of turns and time constants of the various fields. It is particularly important that time-constant and relative-turn ratio of the self-energizing field be correctly proportioned. Assuming a three-field machine, the differential equation of load voltage e will be:

$$e = k_2 i_2 - k_1 i_1 + k_3 i_3$$
$$= i_3 r_3 + L_3 \frac{di_3}{dt} + M_{23} \frac{di_2}{dt} + M_{13} \frac{di_1}{dt}$$

where

k_1, k_2, k_3 are machine constants with various field turns.
i_1, i_2, i_3 are the respective field currents.
r_3 is the self-energizing field resistance.
L_3 is the self-energizing field inductance.
N_{23}, M_{13} are the mutual inductances between fields 2 and 3, and between fields 1 and 3.

By combining this with the equations for other field voltages, the differential equation of voltage build-up in the output circuit can be obtained. Solution of equations of this type are of the forms:

$$e = A_1 \epsilon^{m_1 t} + A_2 \epsilon^{m_2 t} + A_3 \epsilon^{m_3 t} + B$$

where the A's, m's, and B are constants depending on L, R, and M.

By proper proportion of the field circuits, any reasonable speed of response as required for electrode control can be obtained. Optimum performance is obtained when the speed of response of the electrical system is co-ordinated with acceleration characteristics of the mechanical system.

Figure 3 shows an elementary diagram of the regulating system. The current control field of the regulating element is energized from a dry-type rectifier which is in turn connected to a current transformer in the electrode circuit. The potential control field is energized in a similar manner from the voltage between each electrode and the shell of the furnace. In practice, when the breaker in closed voltage is applied to the potential control field which causes the generator voltage to build up in the direction to lower the electrodes. When the first electrode strikes the metal, the voltage drops to zero causing the motor to stop. When a second electrode strikes the metal, a current flows causing the current control field to become energized which acts to raise the electrode thus establishing an arc. As the arc is lengthened, its voltage increases and the current decreases until a balance is established between the potential and current control fields. The

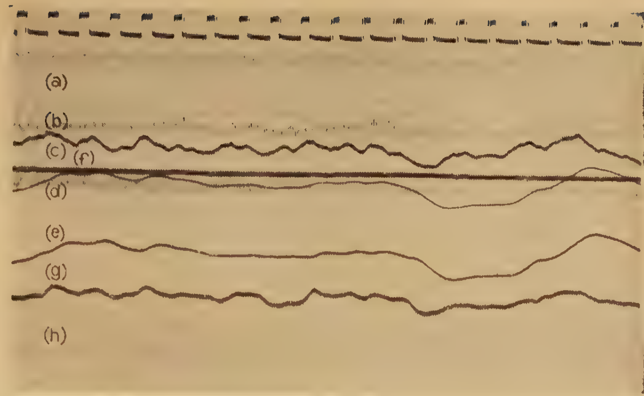


Figure 6. Oscillogram showing electrical performance of regulator during the refining period

(a). Electrode current to Rototrol control field
(b). Rototrol armature volts
(c). A-c volts to Rototrol control field
(d). Motor armature volts
(e). Motor speed
(f). Electrode position indicator
(g). Motor armature current
(h). Electrode current

third electrode is controlled in a similar manner.

As the charge melts and flows away, as the electrodes burn off, and as the scrap falls against the electrodes, the balance between the control fields is continually being disturbed. The Rototrol changes the generator voltage to position the electrodes to maintain the balance between arc voltage and current. This arrangement of the control field circuits corresponds to the accepted principle of regulation used successfully for many years in the balanced-beam type of regulator.

Provision is made for controlling the electrodes manually, either individually or in a group. This facilitates handling of the electrodes during charging, pouring, changing electrodes and so forth.

The wide range of current adjustment required is obtained by means of a rheostat shunting the current transformer. This method is used, since it maintains the same sensitivity for all current settings.

Figure 4 shows the arrangement of equipment in a single metal-enclosed unit. The front panel is arranged for setting into the wall of the furnace vault in the usual manner. Hinged side panels are used on the rear of the unit in order to provide maximum accessibility to the rotating equipment as well as to the panel mounted apparatus. A bolted rear plate is provided so that any one or all of the motor generator sets can be readily removed for inspection or maintenance. Enclosed machines with ball bearings are

A Method for Determining the Normal Modes of Foster's Reactance Networks

WILBUR REED LePAGE
ASSOCIATE AIEE

Synopsis: A method is presented for finding the zeros of the impedance or admittance function of a Foster reactance network. More generally, the method is one of finding the zeros of a rational function having alternate zeros and poles, all of which are simple, if the poles are known; or of finding the poles if the zeros are known. The method consists of writing the function as a sum of partial fractions and replacing all but two of the fractions, depending on the zero to be found, by a constant and solving the resulting quadratic equation. In this way an upper and lower bound of a zero may be found, and, if need be, the method may be applied repeatedly to give a pair of bounds which will be as close to the required zero as necessary. The method has particular value if the rational function is the impedance or admittance function of one of Foster's reactance networks, since these functions are obtained directly from the networks as sums of partial fractions of the form $A_n/(B_n^2-\omega^2)$. Hence, the zeros may be obtained without reducing the function to the polynomial form. The new method may not always be shorter in application than other known methods, but its advantage lies in the facility with which an upper and lower bound of the required zero may be found and in the possibility of dealing with the partial-fraction form of the impedance or admittance function.

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WILBUR REED LEPAGE is an engineer in the advance development department, Radio Corporation of America, Indianapolis, Ind., and was formerly of Cornell University, Ithaca, N. Y.

Statement of the Problem

THE two generalized networks of Figure 1 are Foster's canonic forms, which are of particular importance because either one is a possible representation of any reactance network.[1] They are forms in which the normal modes of oscillation are placed in evidence: for Figure 1a they coincide with the network meshes,[2] and for Figure 1b, which is the dual[3] of Figure 1a, the normal modes coincide with the network nodes. This means that if Figure 1a is excited by a voltage, or Figure 1b by a current, the response frequencies are merely those of the isolated sections. However, if the excitations of these networks are, respectively, current and voltage, they no longer have nodes or meshes which coincide with the normal modes. That is, the value of each natural frequency of oscillation depends on all the circuit constants of the network and cannot be determined from the properties of any one section. The problem to be treated is that of finding the normal modes for these circuits when exposed to the latter type of excitation. These solutions will also include the solutions for certain combinations of these networks, and also the dissipative case where all coils have the same R/L ratio and all capacitors have the same G/C ratio. The method will be illustrated in terms of the voltage excitation of Figure 1b, but with the application of the principles of duality[3] it applies also to the current excitation of Figure 1a. There are known methods for finding these normal modes, but under the special conditions of the circuits considered here the method to be introduced will provide good approximations with a minimum of effort.

Determination of the Normal Modes

On a mesh basis none of the possible meshes of Figure 1b satisfy the necessary conditions to make them coincide with the normal modes,[2] and if there are no such meshes the normal modes, regardless of the actual point of insertion of the disturbing voltage, are the values of p for which

$$Z(p)=0 \tag{1}$$

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used exclusively to eliminate periodic oiling, also to keep abrasive dust out of bearings. Individual motor generator sets having rugged construction and ample safety factors are employed. Should a failure of any unpredictable kind occur, the heat can be continued with two electrodes on automatic control and a spare set placed in service quickly.

The equipment is made so far as possible in one unit so that the various parts can be protected from dirt and physical damage. This also permits factory assembly and complete test, up to the operation of the electrode motors. A minimum of external connections and erection time is thus required.

Figure 5 is an oscillogram showing typical operation of the furnace during the melt-down period. This demonstrates the high speed of the electrodes. Unsustained sudden variations in current cause no extra action or undue wear on the equipment, but sustained changes cause immediate response. It will be noted that the electrode can be withdrawn at relatively high speed when a cave-in takes place. In case of abnormal unbalance between current and voltage in the arc, the equipment is able to perform its function with the minimum number of outages.

The start of the film shows a loss of voltage and current. The motor comes to rest in about one half of a second and remains at rest until the arc is re-established. In this case, the current was low and the Rototrol voltage immediately started rising in a direction to cause the electrode to be lowered. The motor started one seventh of a second after the change was initiated. This delay was due to the electrical and mechanical inertia of the system. The motor was accelerated rapidly to full speed, and, as the current approached the regulated value, the motor speed was diminished. At point 5-*A* there was a marked rise in current and the electrode motor was decelerated to zero speed and accelerated to one-third speed in the raise direction. It should be appreciated that during the melt-down period, electrical conditions in the furnace change rapidly, necessitating more or less continual electrode adjustment. This film covers a period of about ten seconds.

Figure 6 is an oscillogram showing the refining period. Here, relatively small changes in current take place, and consequently only small movement of electrodes is required. Because of the inertia of the electrical system, sudden unsustained variations do not cause movement of the electrodes.

Operating experience has shown that the Rototrol regulator combines the necessary degree of sensitivity, amplification, speeds of response, and stability required for arc-furnace operation. This regulator is particularly successful during melt-down periods when cave-ins frequently occur because the high speed withdrawal of the electrodes prevents breaker tripping because of sustained overload.

where p is the usual generalized angular velocity and $Z(p)$ is the impedance function of the network.[4]

For the network in question,

$$Z(p)=p\left(L_0+\sum_{n=1}^{N}\frac{A_n}{B_n^2+p^2}\right) \tag{2}$$

where

$$\left.\begin{aligned} A_n&=1/C_n && \text{(a)}\\ B_n&=1/\sqrt{L_nC_n} && \text{(b)}\end{aligned}\right\} \tag{3}$$

When there is a series capacitor, as shown in Figure 1b, it is a special case of the parallel combination for which the corresponding $B=0$, and is therefore included in the summation.

For the reactance network the values of p which satisfy equation 1 are pure imaginaries, and accordingly we are interested only in purely imaginary values of p. Therefore, it is convenient to let

$$p=j\omega \tag{4}$$

and

$$Z(p)=Z(j\omega)=j\omega\left(L_0+\sum_{n=1}^{N}\frac{A_n}{B_n^2-\omega^2}\right) \tag{5}$$

It follows from the separation property of zeros and poles[1] of equation 5 that there will be a root of equation 1 between any two adjacent values of B_n. Let the values of B_n be arranged as an increasing sequence, with increasing subscripts, and choose the subscripts on the roots so that

$$B_1<\omega_1<B_2<\omega_2<\ldots<B_{k-1}<\omega_k<B_k<\ldots<\omega_N \tag{6}$$

All B's will be different because it is assumed that Figure 1b is reduced to its simplest form. The solution is obtained by solving

$$L_0+\phi(\omega)=0 \tag{7}$$

where

$$\phi(\omega)=\sum_{n=1}^{N}\frac{A_n}{B_n^2-\omega^2} \tag{8}$$

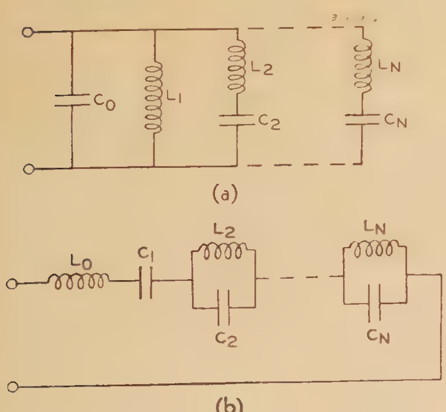


Figure 1. Foster's reactance networks

which contains all the roots except the trivial root $\omega=0$ which occurs if $B_1\neq 0$ and which has been factored out.

This is made clearer by the typical plot of $\phi(\omega)$, as shown in Figure 2. The function closely resembles a reactance function, since the latter is $\omega\phi(\omega)$. The intersections of these curves with the horizontal line $-L_0$ are the required roots. They are located symmetrically about the vertical axis, and therefore it is necessary to consider only the positive roots.

There are two procedures to be followed, depending on whether $1\leqq k\leqq N-1$ or $k=N$.

Case 1. $1\leqq k\leqq N-1$

The root ω_k lies in the interval

$$B_k<\omega_k<B_{k+1} \tag{9}$$

and with this knowledge we can separate out the two terms of $\phi(\omega)$ which have poles nearest to ω_k and approximate the remaining terms by a term which will be a constant. Equation 7 becomes

$$\frac{A_k}{B_k^2-\omega^2}+\frac{A_{k+1}}{B_{k+1}^2-\omega^2}\equiv\psi(k,\omega)=W(k) \tag{10}$$

which serves to define $\psi(k, \omega)$, and where W is a constant. The solution of equation 10 is

$$\left.\begin{aligned}
\omega^2&=\left.\begin{aligned}&\frac{1}{2W}[F(k)+G(k)], && W\neq 0\\ &=\frac{B_{k+1}^2A_k+B_k^2A_{k+1}}{A_{k+1}+A_k}, && W=0\end{aligned}\right\}1\leqq k\leqq N-1 && \begin{aligned}&\text{(a)}\\&\text{(b)}\end{aligned}\\
&\text{where}\\
F(k)&=W(B_{k+1}^2+B_k^2)-(A_{k+1}+A_k) && \text{(c)}\\
&\text{and}\\
G(k)&=\sqrt{(A_{k+1}+A_k)^2-2W(B_{k+1}^2-B_k^2)^2(A_{k+1}-A_k)+W^2(B_{k+1}^2-B_k^2)^2} && \text{(d)}
\end{aligned}\right\} \tag{11}$$

The form of $\psi(k, \omega)$ is shown in Figure 3, and it is seen from this that there may be an extraneous root outside of the interval B_k to B_{k+1}. However, equation 11 is written in such a form as to always give a root within the required interval. In speaking of a root of equation 10 it will always be understood that it is the root given by equation 11. It is evident from the form of $\psi(k, \omega)$ that there will always be a root, regardless of the value of W. The problem is now one of finding suitable values of W. To do this we write equation 7 as

$$\left.\begin{aligned}
&\psi(k,\omega)=S(k,\omega) && \text{(a)}\\
&\text{where}\\
&S(k,\omega)=-L_0-\sum_{\substack{n=1\\ n\neq k,\,k+1}}^{N}\frac{A_n}{B_n^2-\omega^2} && \text{(b)}
\end{aligned}\right\} \tag{12}$$

$S(k, \omega)$ is shown in Figure 3, and it is apparent that in the interval between B_k and B_{k+1}, $\psi(k, \omega)$ is continuous and always increasing, and that $S(k, \omega)$ is continuous, always decreasing, and is bounded. Thus, if ω_p is any value, $B_k\leqq\omega_p\leqq B_{k+1}$, and we define

$$W=S(k,\omega_p) \tag{13}$$

there will be a solution of equation 10 which we will call ω_q and which will have the following properties:

$$\text{if}\ \begin{Bmatrix}\omega_p>\omega_k\\ \omega_p=\omega_k\\ \omega_p<\omega_k\end{Bmatrix}\ \text{then}\ \begin{Bmatrix}\omega_q<\omega_k\\ \omega_q=\omega_k\\ \omega_q>\omega_k\end{Bmatrix} \tag{14}$$

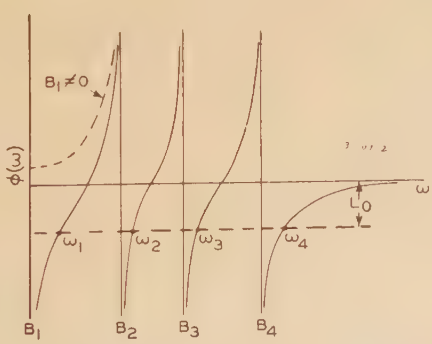


Figure 2. Plot of $\phi(\omega)$ versus ω to show the locations of the roots. Shown for N = 4

The properties of $\psi(k, \omega)$ and $S(k, \omega)$ also show that if $B_k\leqq\omega_p'<\omega_p\leqq B_{k+1}$, and ω_q' is the root corresponding to ω_p', as given by equations 11 and 13, then

$$B_k<\omega_q<\omega_q'<B_{k+1} \tag{15}$$

In view of these relations we will find it expedient to define

$$\left.\begin{aligned} W_1(k)&=S(k,B_k) && \text{(a)}\\ W_2(k)&=S(k,B_{k+1}) && \text{(b)}\end{aligned}\right\} \tag{16}$$

and let the corresponding roots of equation 10 be $\bar{\omega}_k$ and $\underline{\omega}_k$, respectively. From relations 9, 14, and 15 it follows that

$$B_k<\underline{\omega}_k<\omega_k<\bar{\omega}_k<B_{k+1} \tag{17}$$

and we have determined upper and lower bounds of ω_k which are closer than B_k and B_{k+1}. If this approximation is not sufficient, one may use $\underline{\omega}_k$ and $\bar{\omega}_k$ in place of B_k and B_{k+1}, respectively, and determine two new roots, $\bar{\omega}_k'$ and $\underline{\omega}_k'$.

This process may be repeated as many times as required. To be more specific, define

$$\left.\begin{array}{ll} W_1^{(s)}(k)=S[k, \omega_k^{(s-1)}] & \text{(a)} \\ W_2^{(s)}(k)=S[k, \bar{\omega}_k^{(s-1)}] & \text{(b)} \end{array}\right\} s\geq 1 \quad (18)$$

and let $\bar{\omega}_k^{(s)}$ and $\omega_k^{(s)}$ be the roots of equation 10 using equations 18*a* and *b*, respectively, for W. Consider the case $s=1$. From relations 15 and 17 it follows that $\bar{\omega}_k'<\bar{\omega}_k$ and $\omega_k'>\omega_k$, and from relations 14 and 17 we obtain the relations $\bar{\omega}_k'>\omega_k$ and $\omega_k'<\omega_k$. Hence,

$$B_k<\omega_k<\omega_k'<\omega_k<\bar{\omega}_k'<\bar{\omega}_k<B_{k+1} \quad (19)$$

It is obvious that this reasoning may be continued, leading to the pair of sequences

$$\left.\begin{array}{l} \bar{\omega}_k>\bar{\omega}_k'>\bar{\omega}_k''>\ldots>\bar{\omega}_k^{(s-1)}>\bar{\omega}_k^{(s)}>\ldots>\omega_k \\ \omega_k<\omega_k'<\omega_k''<\ldots<\omega_k^{(s-1)}<\omega_k^{(s)}<\ldots<\omega_k \end{array}\right\} \quad (20)$$

Observe that relation 20 contains twice as many values as required to obtain a given approximation. For example, if $\bar{\omega}_k$ is found one can immediately find ω_k', and then $\bar{\omega}_k''$, and so forth. Similarly, ω_k could be used as the starting point, and thus, either of the following pairs of sequences may be used:

$$\left.\begin{array}{ll} \left.\begin{array}{l} \bar{\omega}_k>\bar{\omega}_k''>\ldots>\bar{\omega}_k^{(s-2)}>\bar{\omega}_k^{(s)}> \\ \qquad\ldots>\omega_k \\ \omega_k'<\omega_k'''<\ldots<\omega_k^{(s-1)}<\omega_k^{(s+1)}< \\ \qquad\ldots<\omega_k \end{array}\right\} & \text{(a)} \\ \text{or} & \\ \left.\begin{array}{l} \bar{\omega}_k'>\bar{\omega}_k'''>\ldots>\bar{\omega}_k^{(s-1)}>\bar{\omega}_k^{(s+1)}> \\ \qquad\ldots>\omega_k \\ \omega_k<\omega_k''<\ldots<\omega_k^{(s-2)}<\omega_k^{(s)}< \\ \qquad\ldots<\omega_k \end{array}\right\} & \text{(b)} \end{array}\right\} \quad (21)$$

CASE 2. $k=N$

This case requires special treatment because there is no pole of $\phi(\omega)$ beyond B_n, and ω_N is in the range

$$B_N<\omega_N<\infty \quad (22)$$

In contrast to the previous practice, it is not possible to keep two terms, having poles on opposite sides of ω_N, in their exact form and replace the remainder of $\phi(\omega)$ by an approximation. One could, therefore, keep only one term, $A_N\div(B_N^2-\omega^2)$, as the exact term. However, since the solution can be given explicitly if two exact terms are used, a more accurate approximation will be obtained by also using the term $A_{N-1}/(B_{N-1}^2-\omega^2)$.

One is lead to attempt to approximate the other terms by a constant, as was done in case 1. That is, we will seek a suitable value of W to make the solution of

$$\psi(N-1, \omega)=W(N) \quad (23)$$

approximate ω_N. $\psi(N-1, \omega)$ is shown in Figure 4 from which it is seen that for equation 23 to have a solution it is necessary that $W<0$. Assuming this to be true the solution is

$$\left.\begin{array}{ll} \omega^2=\dfrac{1}{2W}[F(N)-G(N)], \quad W<0 & \text{(a)} \\ \text{where} & \\ F(N)=W(B_N^2+B_{N-1}^2)-(A_N+A_{N-1}) & \text{(b)} \\ \text{and} & \\ G(N)=\sqrt{(A_N+A_{N-1})^2-2W(B_N^2-B_{N-1}^2)^2(A_N-A_{N-1})+W^2(B_N^2-B_{N-1}^2)^2} & \text{(c)} \end{array}\right\} \quad (24)$$

This is written in such a way as to give the solution which is greater than B_N. The root given by equation 24 will henceforth be spoken of as the root of equation 23. Continuing, as for case 1, we write equation 7 as

$$\psi(N-1, \omega)=S(N-1, \omega) \quad (25)$$

and examine $S(N-1, \omega)$ in the interval of ω for which it may serve as a value of W. For $\omega>B_N$, $S(N-1, \omega)$ is continuous and always decreasing, with increasing ω, and becomes less than zero at some point, ω_r, as indicated in Figure 4. This point may lie on either side of B_N. If ω_s is the larger of ω_r and B_N, and if W is defined as

$$W=S(N-1, \omega_t) \quad (26)$$

where $\omega_s<\omega_t<\infty$, then there will be a solution of equation 23 for this value of W. Let this solution be ω_u. From the properties of $\psi(N-1, \omega)$ and $S(N-1, \omega)$ one may conclude that

$$\text{if } \left\{\begin{array}{l} \omega_t>\omega_N \\ \omega_t=\omega_N \\ \omega_t<\omega_N \end{array}\right\} \text{ then } \left\{\begin{array}{l} \omega_u<\omega_N \\ \omega_u=\omega_N \\ \omega_u>\omega_N \end{array}\right\} \quad (27)$$

and if $\omega_s\leq\omega_t'<\omega_t\leq\infty$ and ω_u' is the root corresponding to ω_t', as given by equations 26 and 24, then

$$\omega_s<\omega_u<\omega_u'<\infty \quad (28)$$

The procedure to be used in obtaining a solution depends on the position of ω_r relative to B_N. For the first case, assume $S(N-1, B_N)<0$, which is equivalent to the condition $\omega_r<B_N$. This is similar to case 1 and accordingly we define

$$\left.\begin{array}{ll} W_1(N)=S(N-1, B_N) & \text{(a)} \\ W_2(N)=S(N-1, \infty)=-L_0 & \text{(b)} \end{array}\right\} \quad (29)$$

The corresponding roots obtained by using equations 29*a* and *b*, respectively, will be $\bar{\omega}_N$ and ω_N, and in view of relations 22, 27, and 28 they will satisfy the inequality

$$B_N<\omega_N<\omega_N<\bar{\omega}_N \quad (30)$$

Closer approximations may be obtained, exactly as in case 1, by defining

$$\left.\begin{array}{ll} W_1^{(s)}(N)=S[N-1, \omega_N^{(s-1)}] & \text{(a)} \\ W_2^{(s)}(N)=S[N-1, \bar{\omega}_N^{(s-1)}] & \text{(b)} \end{array}\right\} s\geq 1 \quad (31)$$

and $\bar{\omega}_N^{(s)}$ and $\omega_N^{(s)}$ as the roots of equation 23 using, respectively, equations 31*a* and *b* for W. For $s=1$, in consequence of relations 28 and 30, we have $\bar{\omega}_N'<\bar{\omega}_N$ and $\omega_N'>\omega_N$ and from relations 27 and 30, $\bar{\omega}_N'>\omega_N$ and $\omega_N'<\omega_N$. Therefore we may write the inequality

$$B_N<\omega_N<\omega_N'<\omega_N<\bar{\omega}_N'<\bar{\omega}_N \quad (32)$$

and by extending this repeatedly there results the pair of sequences,

$$\left.\begin{array}{l} \bar{\omega}_N>\bar{\omega}_N'>\bar{\omega}_N''>\ldots>\bar{\omega}_N^{(s-1)}>\bar{\omega}_N^{(s)}> \\ \qquad\ldots>\omega_N \\ \omega_N<\omega_N'<\omega_N''<\ldots<\omega_N^{(s-1)}<\omega_N^{(s)}< \\ \qquad\ldots<\omega_N \end{array}\right\} \quad (33)$$

from which either of the two following pairs may be selected for computation:

$$\left.\begin{array}{ll} \left.\begin{array}{l} \bar{\omega}_N>\bar{\omega}_N''> \\ \ldots>\bar{\omega}_N^{(s-2)}>\bar{\omega}_N^{(s)}>\ldots>\omega_N \\ \omega_N'<\omega_N'''< \\ \quad\ldots<\omega_N^{(s-1)}<\omega_N^{(s+1)}< \\ \qquad\ldots<\omega_N \end{array}\right\} & \text{(a)} \\ \left.\begin{array}{l} \bar{\omega}_N'>\bar{\omega}_N'''> \\ \quad\ldots>\bar{\omega}_N^{(s-1)}>\bar{\omega}_N^{(s+1)}> \\ \qquad\ldots>\omega_N \\ \omega_N<\omega_N''< \\ \quad\ldots<\omega_N^{(s-2)}<\omega_N^{(s)}<\ldots<\omega_N \end{array}\right\} & \text{(b)} \end{array}\right\} \quad (34)$$

With the introduction of the sequences 34*b* there is some relaxing of the original condition, that $W_1(N)<0$. To use these sequences it is necessary only to have $W_1'(N)<0$, which is equivalent to $\omega_N>\omega_r$.

If $W_1'(N)\geq 0$, an upper bound of ω_N may be found by another method which consists of obtaining a root of

$$\psi(N-1, \omega)=-L_0-\sum_{n=1}^{N-2}\frac{A_n}{B_{N-1}^2-\omega^2} \quad (35)$$

It is easy to see that equation 35 has a root, by writing it as

$$\left.\begin{array}{ll} \dfrac{A_{N-1}'}{B_{N-1}^2-\omega^2}+\dfrac{A_N}{B_N^2-\omega^2}=-L_0 & \text{(a)} \\ \text{where} & \\ A_{N-1}'=\displaystyle\sum_{n=1}^{N-1} A_n & \text{(b)} \end{array}\right\} \quad (36)$$

Let this root be designated by $\bar{\omega}_N^*$, and observe that, if $\omega>B_N$, then

$$-\sum_{n=1}^{N-2}\frac{A_n}{B_{N-1}^2-\omega^2}>-\sum_{n=1}^{N-2}\frac{A_n}{B_n^2-\omega^2} \quad (37)$$

From this, and the fact that $\psi(N-1, \omega)$ is increasing with increasing ω, it may be concluded that

$$\bar{\omega}_N^*>\omega_N \quad (38)$$

Thus $\bar{\omega}_N{}^*$, an upper bound of ω_N, has been found by a method which is applicable in all cases.

It is of interest to note, parenthetically, that if $W_1'(N)<0$ then $\bar{\omega}_N{}^*$ and $\bar{\omega}_N{}'$ can both be found, and if there is much difference between them it may be worth the extra computation required to know which is the closer to ω_N and therefore which one may be most profitably used as the basis for future computations of the sequences 34. It is possible to know which is the better approximation by computing $\bar{\omega}_N{}^*$ and then either also computing $\omega_N{}'$ and comparing directly with $\bar{\omega}_N{}^*$, or comparing $S(N-1, \underline{\omega}_N)$ with

$$-L_0-\sum_{n=1}^{N-2} A_n/(B_{N-1}{}^2-\bar{\omega}_N{}^{*2})$$

If the former is larger, then $\bar{\omega}_N{}'>\bar{\omega}_N{}^*$, and vice versa.

To return to the case where $W_1'(N)\geqq 0$, $\bar{\omega}_N{}^*$ having been found, it may be used to define

$$W_2{}^*(N)=S(N-1, \bar{\omega}_N{}^*) \qquad (39)$$

If the root of equation 23, when $W_2{}^*(N)$ is used, is $\underline{\omega}_N{}^*$, it follows from relations 27 and 38 that $\underline{\omega}_N{}^*<\omega_N$. Also, $S(N-1, \bar{\omega}_N{}^*)>S(N-1, \infty)$ and therefore, from the increasing property of $\psi(N-1, \omega)$,

$$\underline{\omega}_N<\underline{\omega}_N{}^*<\omega_N \qquad (40)$$

Although $\underline{\omega}_N<\omega_r$ it is possible that $\underline{\omega}_N{}^*>\omega_r$, in which event $\underline{\omega}_N{}^*$ is a possible starting point for the sequences 34, as a replacement for either B_N or $\underline{\omega}_N$. Whether this condition is fulfilled may be checked by observing if $S(N-1, \omega_N{}^*)$ is less than zero, this being the necessary and sufficient condition to make $\underline{\omega}_N{}^*>\omega_r$.

If this attempt at finding an improved upper bound, by first seeking a lower bound which is greater than ω_r, is not successful, the following procedure may be used. Arbitrarily choose a value, $\omega_N{}^*$, such that

$$\left.\begin{array}{ll}\underline{\omega}_N{}^*<\omega_N{}^*<\bar{\omega}_N{}^* & \text{(a)}\\ \text{and} & \\ S(N-1, \omega_N{}^*)<0 & \text{(b)}\end{array}\right\} \qquad (41)$$

If $\omega_N{}^*$ is less than ω_N, it may be used as B_N or $\underline{\omega}_N$ in forming the sequences 34. To find whether this is true use $S(N-1, \omega_N{}^*)$ as W in equation 23 and call the resulting root $\omega_N{}^{*\prime}$. From relation 27, if

$$\left.\begin{array}{ll}\omega_N{}^{*\prime}>\omega_N{}^* & \text{(a)}\\ \text{then} & \\ \omega_N{}^*<\omega_N & \text{(b)}\end{array}\right\} \qquad (42)$$

Also, from relation 27, if

$$\left.\begin{array}{ll}\omega_N{}^{*\prime}<\omega_N{}^* & \text{(a)}\\ \text{then} & \\ \omega_N{}^{*\prime}<\omega_N & \text{(b)}\\ \text{and if} & \\ S(N-1, \omega_N{}^{*\prime})<0 & \text{(c)}\end{array}\right\} \qquad (43)$$

$\omega_N{}^{*\prime}$ may be used as B_N or ω_N in relation 34. Thus, there are two possibilities: if relation 42*a* is true the search for a lower bound is completed, and the same conclusion follows if relation 43*a* and *c* are both true. However, if relation 43*a* is true but 43*c* is not true then another, smaller, value will have to be assumed for $\omega_N{}^*$.

Application to Other Circuits

In Figure 5 two circuits are shown, to which this method of determining the

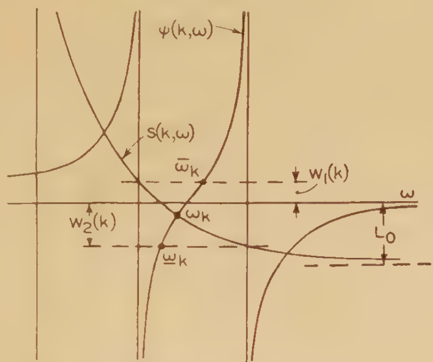


Figure 3. Properties of the functions involved in finding all the roots except the largest

normal modes is applicable. Consider Figure 5b, with a current excitation, a voltage excitation being no different than the previous problem and of no further interest. The normal modes will be the values of p which make

$$Y(p)=0 \qquad (44)$$

From the form of the circuit it is apparent that

$$Y(p)=\frac{Z_A(p)+Z_B(p)}{Z_A(p)Z_B(p)} \qquad (45)$$

and hence, if p_k is a root of

$$Z_A(p)+Z_B(p)=0 \qquad (46)$$

it is also a root of equation 44 unless $Z_A(p_k)=Z_B(p_k)=0$. The roots of equation 46 can be found by the method which has been presented. Likewise, for Figure 5a the normal modes are the roots of

$$Y_A(p)+Y_B(p)=0 \qquad (47)$$

and, as has been indicated, the formal solution of equation 47 is the same as the solution of equation 46.

All of these networks can be treated equally well if all coils have the same R/L ratio and all capacitors the same G/C ratio. The general case of this is treated by Guillemin[5] who shows that if

$$\alpha=R/2L \quad \text{and} \quad \beta=G/2C \qquad (48)$$

then, for roots of $Z(p)=0$, we obtain

$$\left.\begin{array}{ll}p_k=-(\alpha+\beta)\pm j\omega_k & \text{(a)}\\ \text{where} & \\ \omega_k=\sqrt{g_k{}^2-(\alpha-\beta)^2} & \text{(b)}\end{array}\right\} \qquad (49)$$

and where g_k is the value that p_k would have if there were no dissipation.

Application to Other Rational Functions

An impedance or admittance function of a reactance network is a special rational function which has everywhere a positive slope and, consequently, alternating poles and zeros all of which are simple. The poles come in pairs located symmetrically about the point $\omega=0$, and the residue at one of the poles is the negative of the residue at its mate. This makes it possible, for such a function, to combine the terms representing the two poles of a pair into one term and only treat the function for $\omega>0$. The fact that the zeros come in pairs, located symmetrically about the point $\omega=0$, is indicated by equations 10 and 24 because there the zeros are given in terms of ω^2. In the general case of a rational function having alternate poles and zeros, all of which are simple, the same procedure may be used to find upper and lower bounds of the zeros, but without the possibility of combining terms. If the poles and residues are known the approximate zero will be a solution of an equation of the form

$$\frac{A_k}{B_k-\omega}+\frac{A_{k+1}}{B_{k+1}-\omega}=W \qquad (50)$$

where W is defined in similarity with equations 12 and 13 but where the summation must be over all the poles except B_k and B_{k+1}. Equation 50 is a quadratic in ω, and its solution is given by equations 11, but with ω^2 replaced by ω. The treatment for the greatest and smallest zeros will be similar to that given under case 2, with the aforementioned modifications.

It should be pointed out that it is necessary to know only the locations of the poles since the residues may be found by explicit methods.[6] Also, if the locations of the zeros of a function are known, the poles may be found by applying the method to the reciprocal of the function.

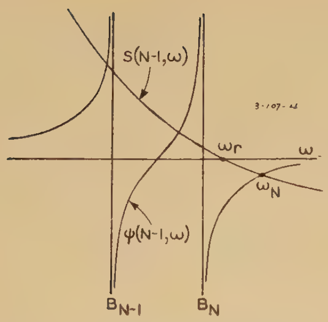


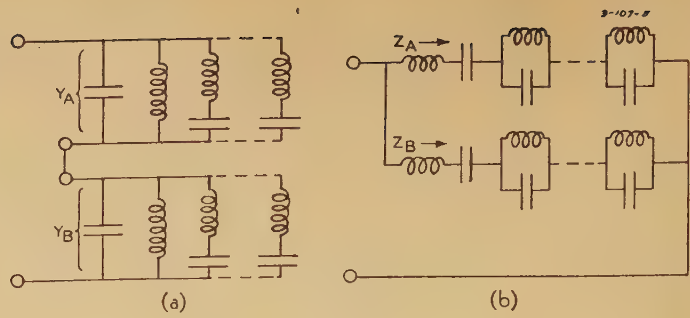
Figure 4. Properties of the functions involved in finding the largest root

Therefore, this method may be used to find the zeros or poles, provided that the poles or zeros, respectively, are known.

Conclusions

In finding the zeros of a rational function by the method presented, one finds a sequence of approximate values, each approximate value being obtained from the previous one. The work involved in finding an approximate value is about the same as in substituting a trial value in the original function, because the evaluation of W requires the substitution of the previous approximate value in all but two of the partial fraction terms which constitute the function. Therefore, probably the greatest advantage of the new method is in the possibility of obtaining, with only two sets of computations, a pair of reasonably close approximate values between which the exact zero is known to be located. In many cases the sequences will converge so rapidly that only two terms need be computed, even for good accuracy. If it is necessary to compute more terms of the sequences to improve the accuracy, this method may have less advantage over the trial method, especially if the initial trials should be close to the zeros to be found. However, the new method does not depend on initial chance assumptions, and it is probable that it will at least yield a solution as quickly, on the average, as the trial method, and perhaps much more quickly. There may be some doubt in the reader's mind as to whether these comments apply to the extreme zeros because of the possible lengthened procedure in this case, which may even require a trial substitution. However, the result of the substitution of this trial value is used directly to find a closer approximation, and also, since a lower and upper bound may always be found by explicit means, the range of choice of the trial value is restricted to the interval between these bounds. This possible increased difficulty in finding these zeros is not without its counterpart in the trial method, because the range of choice of trial values extends to infinity, and hence the probability of a good choice is more remote than for the other more restricted zeros.

Figure 5. Combinations of the reactance networks to which the method is applicable



Of course, the trial method is not the only alternative for finding the zeros of a rational function. They may be obtained by converting the function to the polynomial form and using a method which is applicable to a polynomial, such as Horner's or Graffe's method, or a method suggested by Higgins.[7] However, if the rational function is the impedance or admittance function for the circuits considered here, it will automatically come in the partial-fraction form, and, if there is a large number of terms in the partial-fraction expression, there may be considerable work required to reduce it to the polynomial form. In this case the new method may be preferable, depending on how many of the total number of roots are to be found. This is because the reduction to the polynomial form need be carried out only once, and the work required to find N zeros is therefore less than N times the work required to find the first zero; whereas in the partial-fraction method the work required to find each zero is approximately the same. Hence, the smaller the number of zeros to be found the greater will be the advantage of the partial-fraction method.

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Load Pickup by a Group of Ignitron Rectifiers

L. W. MORTON
ASSOCIATE AIEE

D. I. BOHN
MEMBER AIEE

Synopsis: One of the advantages of the application of mercury-arc rectifiers to large electrochemical loads, such as aluminum cell lines, is that, if all the rectifiers are excited simultaneously, they will pick up and share the load together. This eliminates need for large d-c cell-line circuit breakers.

The conventional method of starting simultaneously several ignitron mercury-arc rectifiers, widely used in large electrochemical plants, has been to energize the individual ignitor firing circuits from a master starting switch. Differences in closing time of the control contactors were reflected in proportional irregularity of load pickup between the ignitron rectifiers.

Recent tests, described in this paper, show that the grids usually employed in the ignitrons for anode shielding can be used as an alternative method to energize cell lines. If all the grids are biased negatively, they will prevent current flowing from the rectifiers while the firing circuits are all energized and the switches closed in the power circuits. Then, by operating one switch or a plurality of accurately timed switches, all grids can be made positive at the same instant with the result that all the rectifiers pick up load at the same instant and share it equally.

MANY of the phenomena of the behavior of large rectifier installations, during the instant of applying or dropping electrochemical loads, have been little understood or investigated by engineers. Certain field tests were made in large aluminum reduction plants, the results of which add to the engineering knowledge relating to this subject. Technical advance in the ever-expanding electrochemical field, even though it involves simply a better understanding of the operation of the electrical apparatus in the plant, is important to the engineer. An analysis of the problem and a report of the field tests are contained in this paper.

Among the big five electrochemicals, aluminum, magnesium, chlorine, copper, and zinc, some of the pioneering work and operating experience in connection with aluminum plants stands out because of the tremendous increase in tonnage of aluminum which industry has demanded, particularly for the airplane program. The electrical apparatus required in the reduction of alumina to pure aluminum in the electrolytic cell lines is, however, typical in a great many respects of what is required for the other electrochemicals.

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L. W. MORTON is electrical engineer with General Electric Company, Schenectady, N. Y., and D. I. BOHN is electrical engineer with the Aluminum Company of America, Pittsburgh, Pa.

Arrangement of Ignitron Rectifiers

For purposes of this paper, it is necessary to review briefly the heavy electrical equipment involved in a typical large rectifier installation, such as is used for the reduction of aluminum.[1–4] Figure 1 shows a typical one-line power diagram for an aluminum installation. Conversion apparatus for one cell line, for instance, is rated 38,700 kw, 645 volts, 60,000 amperes direct current. The individual electrolytic cells are all connected in series, and each carries the full cell-line current, which under normal conditions may range as high as 50,000 to 55,000 amperes. Since it is impractical to build a power rectifier in a single unit of such large capacity, the conversion apparatus must be broken up into several units. So far, in the United States it has been found impractical to build rectifier transformers in the 600-volt d-c output level larger than 10,000 amperes direct current. Likewise, the largest single rectifier assembly in the 600-volt d-c level is approximately 5,000 to 6,000 amperes direct current. For these reasons, the typical 60,000-ampere rectifier installation is, therefore, arranged as shown in Figure 1 with six separate transformers, each feeding two ignitron-rectifier assemblies.

The fact that single large rectifier installations of this sort must be broken up into several units, however, has its advantages. Since there usually is provided a small amount of extra rectifier and transformer capacity, the scheme is flexible from the maintenance standpoint. For instance, it is usually possible to maintain full current input and, consequently, full production when one unit consisting of a transformer and two rectifier assemblies are out of service for maintenance. For the latter reason, and in order to provide protection from faults and arc-back, power circuit breakers and isolating disconnect switches always are provided.

Earlier large electrochemical cell lines, which were placed in operation before the successful development of mercury-arc rectifiers, were fed from several motor–generators or synchronous converters.[5–7] Since the largest ratings in rotating conversion apparatus compare with the present maximum ratings of single assemblies of power-rectifier equipment, earlier arrangements for connecting the several units to the main d-c bus were very similar to current practice.

Problem of Energizing Cell Line

The method used for energizing large electrolytic cell lines from a battery of several generators or rotary converters has a bearing on the subject of this paper. It was first necessary to bring the generators or converters up to speed. Next, their d-c output voltages were equalized. Since it always has been impossible to build high accuracy in the closing time of d-c air circuit breakers, it was necessary to connect all the separate converting units to the main d-c substation bus before the cell-line load was connected to the bus. This requirement existed because the first machine, whose d-c breaker closed, tended to pick up the complete cell-line load. Since the cell-line load is many times the individual rating of the conversion units, its d-c air circuit breaker would immediately trip on overload. The d-c circuit breakers on the rest of the machines also would clear instantly as they

Table I

Location	Test Numbers	Time After Master Starting Switch Was Closed Before the First Rectifier Started	Maximum Difference in Pickup Time Between First and Last Rectifier Starting
Plant *A*	1	4 cycles	1 3/4 cycles
Plant *A*	2	4 cycles	2 1/8 cycles
Plant *A*	3	3 1/8 cycles	1 3/8 cycles
Plant *A*	4	3 1/2 cycles	3/4 cycle
Plant *A*	5	4 1/8 cycles	1 1/4 cycles
Plant *B*	1	4 1/2 cycles	1 1/2 cycles
Plant *C*	1	6 cycles	1 3/4 cycles

In the interval between making plant *A* test 2 and plant *A* test 3, the contactor gap spacings on all 12 ignitor-firing-circuit contactors were readjusted to determine the effects of different contactor closing time on the performance of the various rectifiers. This resulted in changing three factors. First, the time was shortened for picking up the first rectifier. Second, the maximum difference in pickup time between first and last rectifier to start was decreased. Finally, the identity of the first rectifier to pick up load was changed.

closed in succession. For these reasons the method has never been employed.

As an alternative method, a main d-c circuit breaker, capable of closing in on and carrying the full-load line current, was always employed with rotating converting equipment. This latter breaker was closed only after all the individual units had been connected to the station bus and the full capacity required to carry the load continuously was available.

Obviously, the elimination of this expensive circuit breaker designed to carry and rupture these large and highly inductive loads is desirable.

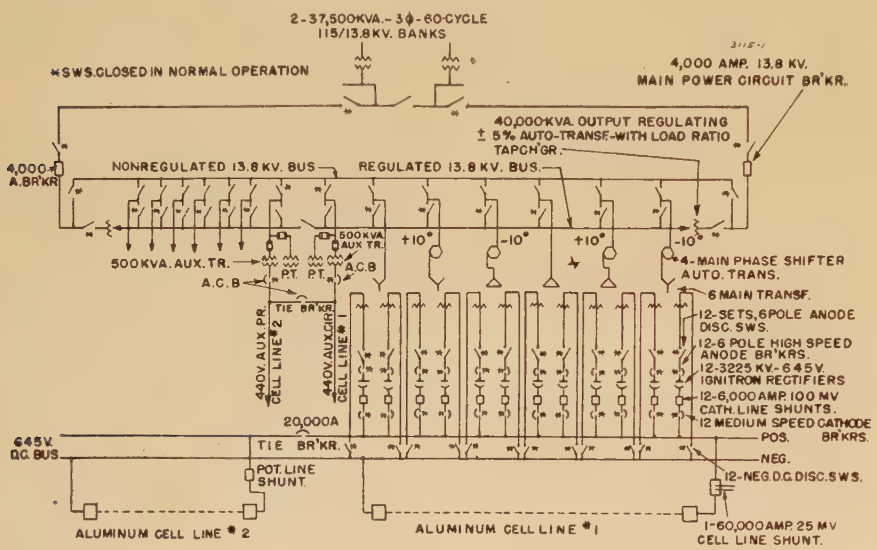


Figure 1. Typical one-line power diagram for cell lines, Aluminum Company of America

One of the advantages of mercury-arc rectifiers over rotating equipment is their ability to pick up electronically such large loads after all the power switching has been completed. There are two general ways of accomplishing this:

1. By controlling the arc in the rectifier with grids.
2. By controlling it with ignitors.

Load Application by Ignitor Control

A widely used method of energizing large cell-line loads supplied by ignitron rectifiers has been to close the a-c and d-c power circuit breakers before exciting the ignitors in the cathode mercury. This means that no current flows from the rectifier, even though all the power circuits are closed, because the ignitors have not caused cathode spots. Magnetic-impulse firing circuits are used widely in such electrochemical service.[8,9] Most magnetic-impulse firing circuits commercially available start generating impulse voltages almost instantly after the contactors in their supply lines are closed and suitable a-c voltage is applied. Advantage has been taken of this performance of the ignitor firing circuits for starting all the rectifiers in a substation at once in order to pick up the large cell-line current.

Where as many as 12 ignitron rectifiers feed a common d-c bus, a master a-c excitation circuit has been used. This has consisted of a 12-pole relay or the equivalent. Each of the 12 poles is connected to the closing coil of a three-pole contactor in the a-c supply circuit to the firing equipment of each of the rectifiers. A single-pole master control switch located in the coil circuit of the 12-pole relays can, therefore, be closed to pick up all of the 12 separate a-c contactors in the supply circuits to the firing equipments.

The procedure, therefore, in picking up ignitron-fed cell-line loads after outages has been rather simple. First, the master excitation switch is opened, which releases all the a-c contactors in the individual firing-circuit supplies. Under this condition, none of the ignitors in any of the rectifiers can create cathode spots to cause current to flow. Next, all of the a-c and d-c power circuit breakers and switches are closed. So far no current flows. The final step then is to close the master a-c excitation circuit energizing all of the separate ignitor firing circuits.

Limitations of This Widely Used Starting Method

The master a-c excitation method of picking up large electrochemical loads fed from ignitron rectifiers has been used for years. As operating experience was gained, it appeared, however, that there were certain limitations. Outages have occurred occasionally because of loss of main a-c supply voltage. Sometimes, when the cell lines were re-energized, one or more of the cathode breakers would trip out from overcurrent. There appeared to be two possible causes for this action:

1. Standard commercial a-c contactors (three-pole) were used in the supply circuits to the individual ignitor firing equipments. While the pickup time of these contactors for most applications is satisfactorily uniform, it was realized that some disparity must exist between the various contactors in closing action. Since cell lines have inductance, it had been anticipated by the application engineers first dealing with the problem that the rate of rise of the cell-line current would be low enough to permit such inequalities in contactor closing time as might appear. Whether this assumption was correct now seemed open to some doubt until tests could be made to substantiate it under field conditions.

2. Resistance characteristics of ignitor firing points are different when the ignitors and liquid mercury in the cathode pools are cold from what they are during operation when the ignitors and mercury are hot. It appeared possible that when the ignitors were cold, such as is the case after a prolonged shutdown, there might be some delay in creating cathode spots in the ignitron rectifiers after the ignitor firing circuits were energized, until the ignitors warmed up and their resistance characteristics improved.

Grid Starting Method

A careful study of the performance of the rectifiers and ignitor firing circuits, was, of course, indicated under the circumstances. It was obvious that satisfactory data could be obtained only under actual field conditions. In anticipation that one or the other, or both, of the foregoing limitations would prove true, an alternative method for starting large ignitron installations feeding electrochemical loads was designed.

Use of Anode Baffle for Grid Control

All large pumped ignitron rectifiers employ a device which in principle is somewhat analogous to the grid in a multianode-type rectifier, but which usually has been called an anode baffle or shield when used in ignitrons. Figure 2 shows the cross section of a single-anode ignitron assembly. Please note the structure surrounding the anode which is called the deionizing anode baffle. This baffle, which is constructed of graphite and insulated from the metal vacuum-tight envelope, is energized through the grid seal. Its function in the ignitron-type rectifier has been to shield the main anode, thereby preventing or minimizing

the occurrence of arc-backs. The fact that it was available, however, suggested that it might be used as another means of controlling the arc.

Factory tests were made to determine whether, with the right negative-bias voltage supplied to these grids, as they should be called when used for such purposes, sufficient control could be obtained to prevent current flowing from the anodes even though the ignitors were energized and firing. These tests indicated that there was enough grid action in the present-type ignitron-insulated anode baffle to prevent current flowing, when the baffles were biased negative with respect to the cathode by 300 volts, under expected conditions of operating temperature and positive main-anode voltages likely to be encountered in this field of application. Satisfactory blocking control could be obtained at this negative-bias level with as many as 500 ohms in series with the biasing circuit. This meant that a practical scheme for grid blocking the main arc in the rectifier could be developed.

Therefore, a cell-line starting circuit based on grid-blocking action in ignitron rectifiers was built. Figure 3 shows the schematic arrangement of a typical grid-bias starting circuit.

Procedure for Energizing Cell Line by Grid Blocking

A procedure for re-energizing a large ignitron rectifier installation after an outage using the Figure 3 circuit is as follows:

1. The 350-volt d-c bias rectifier is energized by closing the fused knife switch and contactor *A* in its supply circuit. In normal operation, the station operator energizes it by closing switch C_1 which is located on his master operating panel.

2. When operator's switch C_2 is closed, all *31S* relays close their contacts, and all the *31SX* relays open their contacts. This action then applies negative 350-volt d-c bias between the cathodes of all the rectifiers and the grids. It is now impossible for any of the rectifiers to carry current so long as this negative bias is maintained.

3. All power circuit breakers and disconnect switches are next closed connecting the rectifiers to both a-c supply voltage and the main d-c station bus.

4. The operator's master a-c excitation switch is then closed, which energizes all the individual firing circuits and causes all the ignitors to start firing. However, no current flows from the rectifiers, because the negative bias is maintaining a blocking action.

5. Finally, the short-circuiting switch *D* is closed, instantly removing the negative bias from all the grids in all the ignitron rectifiers. There remains the 110-volt positive a-c excitation from the baffle transformers, which had been canceled up to this time by the 350-volt negative bias. All 12 ignitron rectifiers, therefore, are permitted to carry current at the same instant.

6. Operator's switches C_1 and C_2 can now be opened. This de-energizes the 350-volt negative-biasing circuit, disconnecting it completely from the main power circuits. It also operates all relays *31S* and *31SX*, transferring the neutrals of the baffle transformers from the master biasing circuit back to the cathode terminals of the rectifiers.

Field Tests

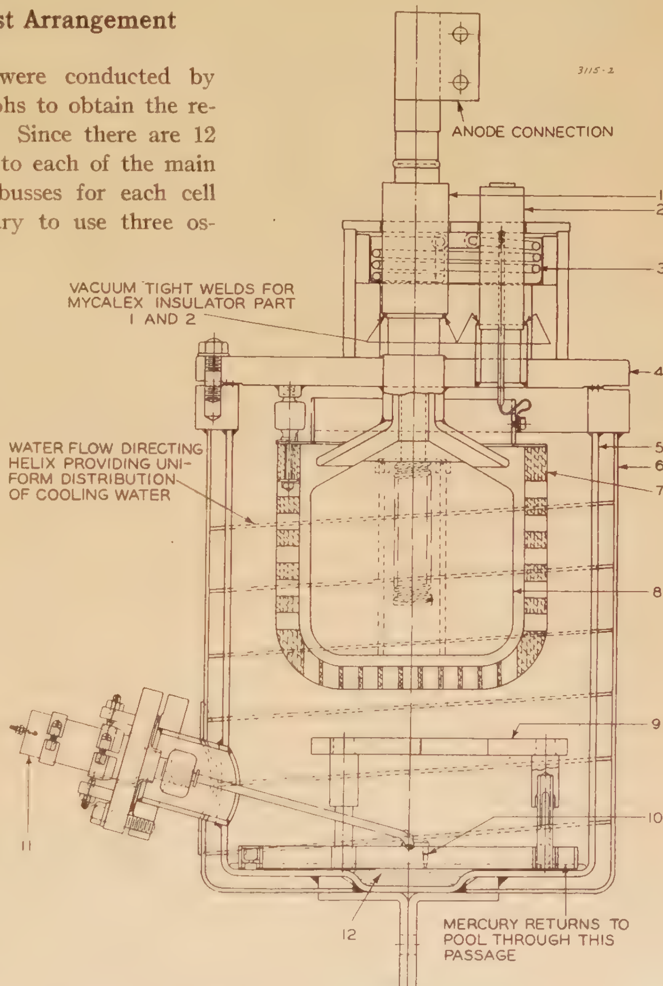
In order to make a thorough study of the problem, it was decided to make extensive tests in the field. Aluminum Company of America operates several reduction plants. Three western plants, all employing rectifiers, were selected. These plants will be designated as plants *A*, *B*, and *C*, for purposes of reference. Initial tests were made on one manufacturer's rectifiers at plant *A*. Confirming tests were made on rectifiers of other manufacture and type at plants *B* and *C* to make them fully comprehensive in nature.

Oscillographic-Test Arrangement

The field tests were conducted by means of oscillographs to obtain the required information. Since there are 12 rectifiers connected to each of the main 60,000-ampere d-c busses for each cell line, it was necessary to use three oscillographs in order to have available enough vibrators to record the behavior of the cathode currents in all 12 rectifiers. By using three oscillographs, 20 vibrators were available.

The three oscillographs were placed on a single operator's table. One vibrator in each oscillograph was connected to a common timing wave, so that the instant of starting the 12 rectifiers would be commonly indicated on the three oscillograms taken for each test. Then four supersensitive vibrators of oscillograph 1 were connected through shunt leads to the ammeter shunts in the cathode lines of rectifiers 1*A*, 1*B*, 2*A*, and 2*B*. Oscillograph 2 had four supersensitive vibrators connected to the four ammeter shunts in cathode lines of 3*A*, 3*B*, 4*A*, and 4*B*, and likewise oscillograph 3 was connected with ammeter shunts in cathode lines of 5*A*, 5*B*, 6*A*, and 6*B*. In each instance, main cell-line current was recorded on one of the films.

Control of the starting operation for all 12 rectifiers was transferred from the operator's main panel in the substation to control switches on the oscillographer's



1. Mycalex anode insulator
2. Mycalex insulator (lead to insulated baffle)
3. Anode heater
4. Vacuum-chamber cover
5. Vacuum chamber
6. Water jacket
7. Energized (deionizing) anode baffle
8. Graphite anode
9. Mercury splash baffle
10. Ignitor tip
11. Mycalex insulator for ignitor and relieving anode leads
12. Mercury pool (cathode)

Figure 2. Cross section of single-anode ignitron assembly

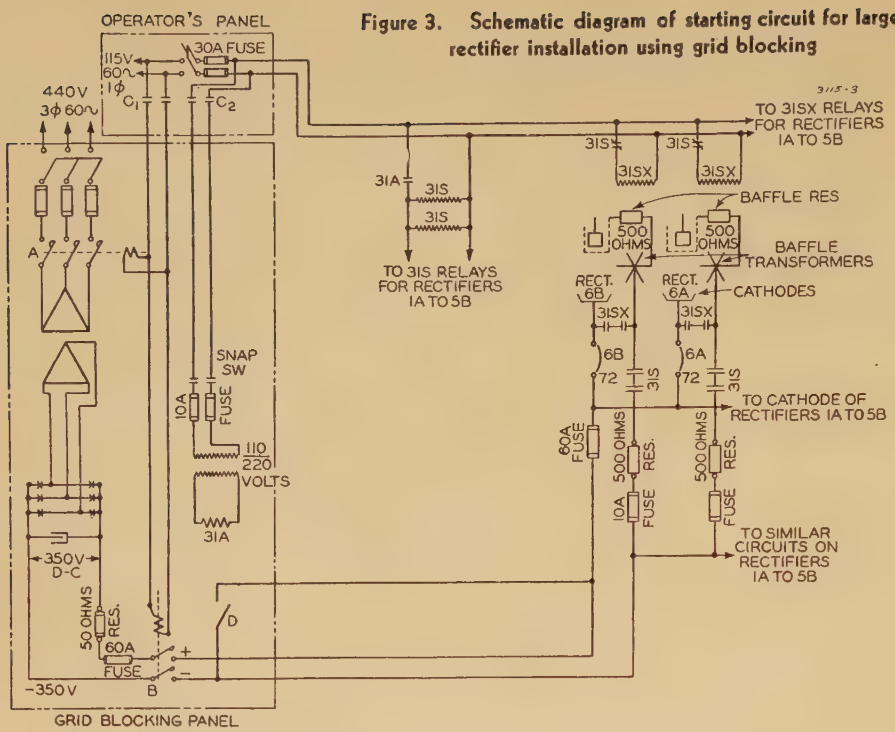


Figure 3. Schematic diagram of starting circuit for large rectifier installation using grid blocking

Figure 4 (below). Typical oscillogram starting cell line by master a-c excitation

On this test the oscillograms for the cathode currents of the other eight rectifiers showed the following results:

1*A* started 0.72 cycle late

1*B* started 0.60 cycle late

2*B* started 0.67 cycle late

3*A* started 0.64 cycle late

5*A* started 0.33 cycle late

5*B* started 0.33 cycle late

6*A* started 0.72 cycle late

6*B* started 0 cycle late same instant as 3*B*. Cathode current for 6*B* overswung same as 3*B*. Otherwise behavior of cathode current for these rectifiers was same as shown for 2*A*, 4*A*, and 4*B*

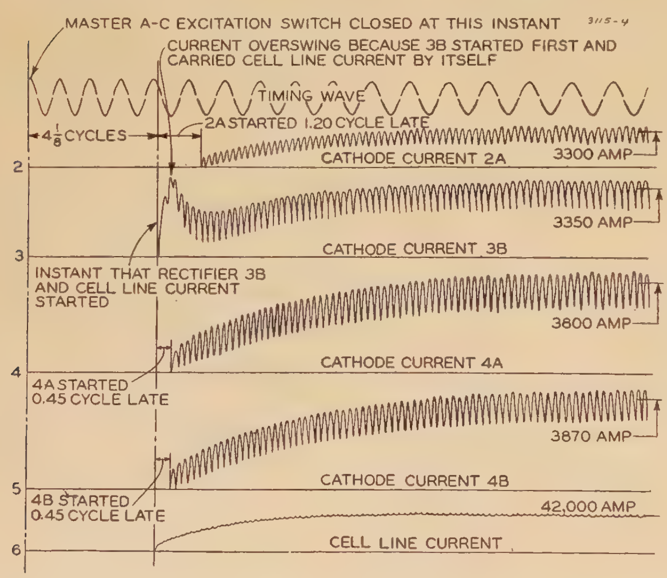


table. All 12 rectifiers would be first taken out of service simultaneously by the station operator. Then, after switching changes were completed quickly, the oscillographer, who controlled the single final starting operation, gave an audible signal, whereupon all three oscillographic instruments were started. The oscillographer, who gave the signal for starting the three oscillographic instruments, operated the master starting circuit for the 12 rectifiers a fraction of a second after giving the signal. This test setup and procedure worked remarkably well. The instruments were synchronized so accurately by their operators that the starting transients occurred on the oscillographic film for all tests less than 30 cycles after the starting signal. At the three plants a total of 13 such tests was made.

Results of Starting Tests Using Ignitor Control

Five tests were made at plant *A*, two at plant *B*, and one at plant *C* to record the behavior of the 12 separate rectifiers using the master a-c ignitor-excitation method. Figure 4 is typical of the oscillographic results of these tests. The number of cathode-current records shown in Figure 4 is limited to only 4 of the 12 rectifiers. This was necessary, because an illustration showing the behavior of all 12 rectifiers would be excessively large for publication. However, these four records typify the results.

The results of several of the starting tests using the master a-c ignitor-excitation method to demonstrate the effects or disparity in starting time of the individual rectifier units are given in Table I.

A special test was made at plant *B*, using the master a-c ignitor-excitation method to determine what effect cold ignitors and cathode mercury would have on starting. The volt-amperes output characteristic of the firing circuit used on these rectifiers was lower than on the rectifiers where the other starting tests with master a-c excitation were made. This volt-ampere characteristic was, however, sufficiently high to give satisfactory operation of the ignitors under continuous normal operating conditions after the ignitors and cathode mercury were warm. Results of this special test were that some of the rectifiers delayed as long as 20 to 30 cycles before the ignitors began to fire. Needless to say, with the

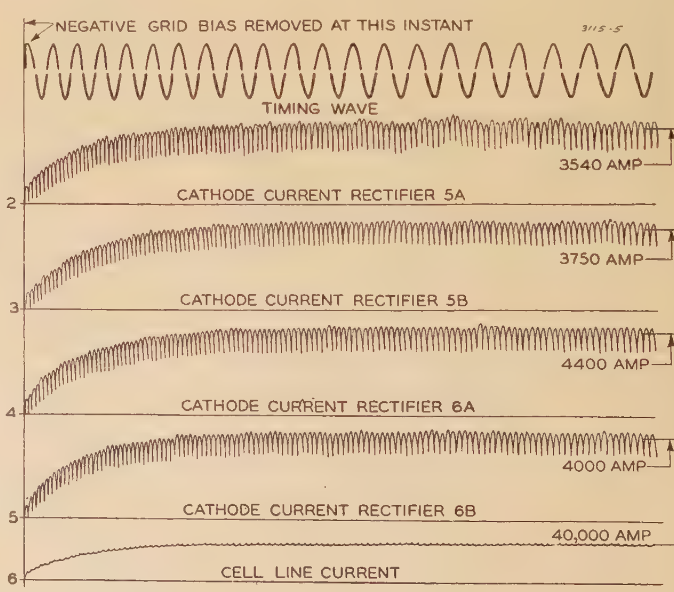


Figure 5. Typical oscillogram starting cell line by grid blocking

Cathode currents for rectifiers 1*A*, 1*B*, 2*A*, 2*B*, 3*A*, 3*B*, 4*A*, and 4*B*, started at same instant and had same appearance as cathode currents for 5*A*, 5*B*, 6*A*, and 6*B* shown on this oscillogram

various individual rectifiers trying to pick up a cell line at various intervals over such a long period as 20 cycles, each of them tripped out on d-c overcurrent in turn as they started, and the rectifiers failed to energize the cell line.

Results of Starting Tests Using Grid Method

There were five cell-line starting tests made using the Figure 3 grid-blocking methods, two at plant *A* and three at plant *B*. The results of the tests are typically illustrated by Figure 5. For the same reasons as explained for Figure 4, only 4 records of cathode current are shown instead of 12.

During the tests with the grid blocking, the power circuits were all energized and the ignitors caused to fire for a short interval prior to the time when the negative grid bias was removed to pick up the cell-line load. There was no failure of blocking action at any time.

Conclusions

Based on the results of the various tests described, the following conclusions were reached:

1. While the cell-line current requires five to six cycles to reach its steady-state value, its initial rate of rise, while slow referred to the total load, is relatively high referred to the rating of a single rectifier. For this reason, unless all the rectifiers start carrying current at the same instant, when a cell line is energized, the first rectifiers to carry current accumulate load so rapidly that overload can exist in a cycle or less. The three-pole contactors used for the supplies to the individual ignitor-firing circuits were of a standard industrial-control type. A disparity as high as $2^1/_8$ cycles existed in the closing times of these contactors, as proved by the various starting tests. In view of the high rate of rise of cell-line current, if the conventional master a-c ignitor-excitation method of cell-line starting is to be used successfully (excluding effects of cold ignitor points and cold mercury), only contactors having a maximum difference in closing time not greater than one-half cycle should be used.

2. It was proved that the resistance characteristics of cold ignitors may be such that they require substantially higher volt-amperes to cause them to ignite cathode spots, when cold, after a service outage, than under normal operating conditions. Therefore, to insure satisfactory cell-line starting with ignitron rectifiers, the volt-ampere characteristics of the firing circuit must match these extra requirements if the master a-c excitation method is to be employed.

3. The new starting method for large rectifier installations employing the grid-blocking action available in ignitron rectifiers was demonstrated to be practical. The equipment used for making the starting tests by the new grid-blocking method was semi-permanently connected in one of the ignitron-rectifier cell-line installations at plant *B*. At least six emergency outages have occurred subsequent to the tests described in the paper. In each instance the new grid-starting method was employed for re-energizing the cell line. All reports show that the scheme was completely successful in all cases. It is, therefore, believed that the grid-blocking method offers a better solution for starting large Ignitron installations of this sort. This conclusion may be reached, because the new method overcomes the difficulty with cold ignitors by permitting the station operators to run them for a sufficient period to warm them up and restore easy ignition characteristics; and it also eliminates the problem of obtaining control contactors having precision closing time, the outcome of which, at best, may be doubtful.

Addendum

During the tests, in addition to solving the problems outlined in the paper, several other interesting and valuable data were obtained. The effect of cell-line inductance in the operation of large electrochemical plants often has been speculated upon by engineers dealing with the problem. Records were obtained of the cell-line-current rate of rise and are shown in Figure 4 and Figure 5.

Cell-line inductance as measured by the starting transients in cell-line current, shown in Figure 4 and Figure 5, has been recognized as a factor affecting the performance of main cell-line breakers in the older installations using rotating apparatus for conversion purposes, and also the cathode breakers, which are used to trip the cell-line currents when shutting down installations employing mercury-arc rectifiers. While it is well known that large air circuit breakers trip with more uniform opening time than they close, nevertheless, the contacts are bound to start parting on some of the cathode breakers earlier than others during planned shutdowns.

Large electrochemical rectifier installations are shut down by operating a master switch which gives simultaneous tripping impulses to all the cathode breakers. When the faster cathode breakers start parting contacts, there is a rapid extinction of current from their associated rectifiers. The remaining rectifiers, whose cathode breakers are slower to open, therefore, accumulate the portion of the load dropped by the earlier rectifiers.

Two important effects, therefore, combine to impose substantially higher interrupting duty on the slower cathode breakers. First, overcurrent exists on one or more of the rectifiers. The magni-

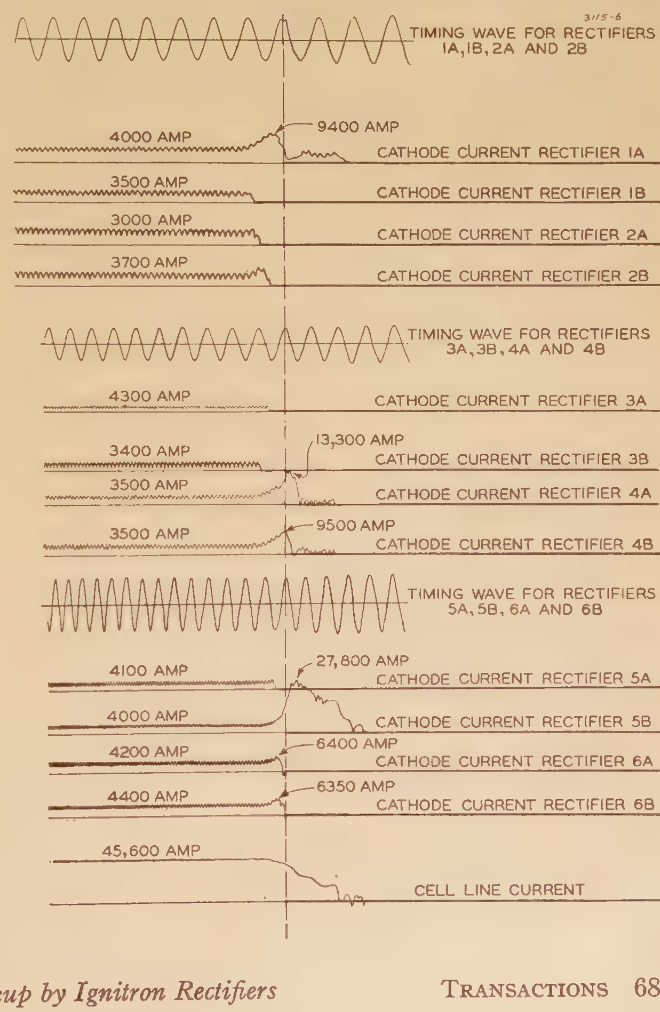


Figure 6. Typical oscillogram showing cathode current in all 12 rectifiers feeding one cell line and total cell-line current when cell line is de-energized by tripping cathode breakers by master tripping switch

The Quadrature Synchronous Reactance of Salient-Pole Synchronous Machines

R. V. SHEPHERD
MEMBER AIEE

C. E. KILBOURNE
MEMBER AIEE

Synopsis: Advances in the theory of synchronous machines have created a number of reactance constants, and improvements in theory have led to continual refinements in methods of calculation and test. This paper presents an improvement in the calculation of the quadrature synchronous reactance, compares calculated with test results, and discusses four methods of test, one of which is an improvement in simplicity and ease of accomplishment over methods of test presented previously.

THE wide application of synchronous machines to many methods of power generation and utilization has caused the development and refinement of synchronous-machine theory as a tool to enable the accurate prediction of the behavior of these machines. This theory has created a number of characteristic constants, or coefficients, in terms of which the operation of the synchronous machine is defined. Papers dealing with one or more of these constants have appeared in the pages of the technical press from time to time, each paper broadening the general concepts or presenting methods of calculation or test which more sharply define particular constants.*

Past attention to individual reactance constants has been devoted chiefly to the armature leakage reactance[7] x_l, to the direct-axis quantities[2,6] x_d, x_d', x_d'', and to the negative- and zero-sequence reactances[2,6] x_2 and x_o. Less attention has been paid to the quadrature synchronous reactance[1,2] x_q either in the development of its full theory or in the presentation of test data. The situation is a natural one, as this reactance is not of major importance in either the short-circuit or starting characteristics of machines, and it is these characteristics which have received the most attention. Further, both the classic slip test[6] for x_q and reluctance power test[1] present difficulties in their execution which have discouraged the accumulation of data.

The quadrature synchronous reactance is of prime importance in determining the steady-state torque-angle characteristic which in turn evaluates the spring constant P_r. Accurate knowledge of either or both of these things is necessary when combining synchronous machines with mechanical drives having pulsating shaft torques such as are found in Diesel or gasoline engines and reciprocating compressors. The present tremendous increase in the use of Diesel and gasoline engines therefore makes an analysis of x_q particularly pertinent.

This paper presents refinements in the theory and calculation of x_q and advances two new methods of test for this quantity. It also presents a discussion of the two previously published test methods and compares the results which have been obtained by calculation and by the four test methods.

General Theory, Methods, and Test Results

The quadrature synchronous reactance x_q of salient-pole synchronous machines can be thought of as being made up of two major parts. One of the parts of this quantity is armature leakage reactance x_l the theory of which has been fully presented previously. The second part of this quantity is the reactance of armature reaction. This second quantity x_{aq} exists as a result of the generation of fundamental air-gap flux when armature current flows and the subsequent generation by that flux of a component of armature voltage which opposes an increase in the armature current. The magnitude of the flux and therefore the back voltage depend in part upon the permeance of the air gap opposite the armature magnetomotive-force wave.

In synchronous machines there are two axes of air-gap symmetry, the direct and quadrature axes, and hence two values of gap permeance. Each of these has been determined accurately over the range of variables encountered in modern machine design for unslotted air gaps, but the effect of slots in either or both the armature and field surfaces, while it has been recognized, has not been presented previously in the technical press. Usually the reactance of armature reaction constitutes the major part of x_q, and to predetermine this quantity accurately, its major component must be predetermined

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R. V. SHEPHERD and C. E. KILBOURNE are both of the motor and generator engineering department of General Electric Company Schenectady, N. Y., where Mr. Kilbourne is designing engineer.

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* See "References."

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tude of this current may reach as high as 600 per cent of the full-load current rating of an individual rectifier. Second, the high inductance of the cell-line load tends to maintain current through the breakers.

The opportunity was seized, during the tests described in the paper, to make some records of the behavior of current supplied by the various rectifiers to the d-c bus during cell-line shutdown. The results of these tests are shown in a typical fashion by Figure 6.

Rectifier cathode-current traces in Figures 4, 5, and 6 indicate a high 360-cycle d-c current ripple. Actually, this ripple is substantially lower. It was necessary to use standard d-c shunts instead of oscillograph shunts. With standard shunts the effect of shunt inductance, as well as the effect of fields on shunt leads, is responsible for this apparently large ripple. This must be taken into account in analyzing the oscillograms.

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9. EXCITATION CIRCUITS FOR IGNITRON RECTIFIERS, H. C. Myers, J. H. Cox. AIEE TRANSACTIONS, volume 60, 1941, October section, pages 943–8.

accurately. In Appendix B a method of calculating the quadrature reactance of armature reaction is presented. This method is based on obtaining the unslotted quadrature-axis air-gap permeance in the conventional manner and then applying to it selected fringing coefficients to account for the slots in the magnetic surfaces. After a value for the reactance of armature reaction has been obtained, the armature leakage reactance is added to it to obtain x_q. The results obtained by calculation are substantiated, as shown in Table I, by several different methods of test.

Unlike the method used for calculating x_q, the test for it must measure the entire quantity, both the armature leakage reactance and the reactance of armature reaction, as it is impossible in test to separate x_q into its analytical components and measure each individually. This restriction places on all test methods the necessity of measuring terminal conditions at an instant when these conditions can be used to evaluate x_q. This instant always occurs just prior to a state of instability, and consequently accurate measurements are not easily made.

In the first method of test, the slip test of Appendix C, an oscillograph is used to record the terminal voltage and armature current as the rotor is forced to slip past the axis of armature magnetomotive force. Unfortunately, the very slip which makes the test possible introduces its own errors. If the slip is large, over $1^1/_2$ per cent, currents are often induced in the usually present amortisseur windings, and the armature current is influenced by them, so that it may no longer reflect the true rotor permeance. While smaller values of slip can be obtained, they require accurate control of the terminal voltage and ability to adjust it, usually over the range from 20 per cent to 30 per cent of normal voltage. This control and adjustment is seldom available anywhere except on the manufacturer's premises, and it is not always easy to obtain it there for every machine.

The reluctance-torque method, described in Appendix D, is based on the determination of the maximum power the machine will carry when operating as a reluctance motor. The motor, with field open-circuited, can be pulled out of step either by increasing the shaft load with terminal voltage held constant or by reducing the terminal voltage with the shaft load held constant. Since the measurements are always made at the moment of instability, it is important in both of the preceding methods that the shaft load contain no torque pulsations. If the adjustable-shaft-load method is used, it is important that the load be capable of fine adjustment, so that the approach to the point of instability can be made accurately. If the test is taken at no load, the line voltage is reduced until the machine falls out of step because of the friction and windage load. This requires the same type of terminal voltage control necessary in the slip test and hence presents the same difficulties. In addition, it also requires accurate measurement of the friction and windage load.

The negative-excitation test, described in Appendix E, is based on the determination of the amount of field excitation required to overcome the reluctance torque of the machine. The test is taken with the machine operating as a synchronous motor with a free shaft and at rated line voltage, thus eliminating the necessity for a variable voltage supply. It is important in this test that the shaft load be free of torque harmonics and of as small a magnitude as it is possible to obtain. As this small load determines the point of instability, it is important that it be known to calculate x_q.

The fourth and most satisfactory test method, the maximum-lagging-current test, described in Appendix F, uses the same method of test as in the negative-excitation method of Appendix E. In this case it is not necessary to measure shaft power, other than to be sure that it is small, as line voltage and current readings are sufficient. Because the line current is large in size and easy to measure, the test is simpler in its technique than previous methods. Further, a variable line voltage is not required, because, like the negative-excitation test, results can be obtained at rated voltage. The value of x_q is simply the ratio of voltage and maximum stable current.

Thus, in the course of developing various methods of test, the objections have one by one been removed. The necessity of having an oscillograph was first removed, then the requirement of a controlled low-voltage source, and finally the low power readings. The results obtained by the various methods of test are shown in Table I. It was not possible to test each machine included in the table by every test method, and the blanks indicate lack of test data on the particular machine. The agreement is, in general, excellent. No attempt has been made to establish divergences between test and calculation or between test methods. These are apparent from a study of the table itself.

Table I. Quadrature Synchronous Reactance x_q by Calculation and Test

					x_q				
						Tests			
Type of Machine	Kilovolt-amperes	Volts	Frequency (Cycles Per Second)	Rpm	Calculation	Slip	Reluctance Torque	Negative Excitation	Maximum Lagging Current
Motor	1,405	4,160	60	1,800	0.98	1.03	0.98	1.00	0.96
Motor	300	440	60	1,200	1.18		0.97	1.26	1.16
Motor	380	440	60	1,200	1.14	1.09	1.11	1.10	1.14
Industrial-type generator	31.3	208	60	900	0.43	0.42	0.41	0.39	0.43
Motor	900	4,150	60	900	0.98	0.91	1.03	0.98	0.98
Motor	2,700	6,600	60	900	0.98	1.04	1.02	1.00	1.00
Motor	1,160	2,200	60	900	0.74	0.77		0.77	0.72
Motor	1,250	6,600	60	900	0.53	0.54	0.59	0.58	0.57
Motor	1,358	4,100	60	900	0.83	0.83	0.80	0.82	0.82
Motor	2,200	2,200	60	720	0.72	0.71	0.68	0.70	0.70
Motor	1,175	2,200	60	600	0.75	0.77	0.83	0.81	0.82
Motor	5,900	6,600	60	514	1.02	0.96	1.02	0.97	0.98
Motor	2,100	6,600	25	500	0.92	0.92		0.92	0.93
Motor	1,465	2,200	60	450	0.82	0.82		0.84	0.82
Motor	690	4,000	60	400	0.84	0.86	0.85	0.87	0.85
Motor	7,320	6,900	60	400	0.43	0.43			
Industrial-type generator	187	400	50	375	0.53	0.63	0.57	0.63	0.66
Industrial-type generator	500	4,160	60	360	0.60	0.58	0.59	0.57	
Industrial-type generator	625	4,160	60	360	0.65	0.67	0.65	0.67	0.66
Industrial-type generator	375	2,400	60	327	0.75	0.73	0.71		
Motor	205	2,200	60	300	0.99	1.00	0.89	0.95	0.93
Motor	1,380	4,000	60	277	0.77			0.70	0.74
Waterwheel-type generator	6,250	4,150	60	240	0.55	0.57	0.59		
Motor	780	2,300	60	164	1.08	1.09	0.98	1.10	1.08
Waterwheel-type generator	24,000	13,200	60	164	0.58	0.58			0.59

Conclusions

The paper has presented improvements in the method of calculating x_q which enable the accurate prediction of this quantity. It has reviewed two well-known test methods and presented two new ones, one of which, the maximum-lagging-current test, has great advantages in simplicity of technique and ease of accomplishment. These advantages should lead to a wide acceptance of the maximum lagging-current test as a recognized test procedure.

Appendix A. Nomenclature

Electrical quantities, voltage, current, power, reactance, and resistance, are in the unit system and are represented by symbols which have already been generally recognized. Other quantities are, when necessary, defined adjacent to their initial use in the text, and all definitions are consolidated in this appendix.

A = square wave value of armature-reaction ampere turns per pole

$$A_1 = \frac{\text{maximum ordinate of fundamental flux-density wave}}{\text{maximum ordinate of actual flux-density wave}}$$

in the air gap of a synchronous machine when excited only from the rotor (saturation is neglected)

$$A_{q1} = \frac{\text{maximum ordinate of fundamental flux-density wave}}{\text{maximum ordinate of actual flux-density wave that would be produced by the same magnetomotive force acting on a uniform gap equal to } g_{\min}}$$

in the air gap of a synchronous machine when excited by sine wave of armature magnetomotive force in the quadrature axis (saturation is neglected)

C_{gfdf} = total fringing coefficient applied to g'_{fdf} to obtain g_{fdf}

C_{gfqa} = total fringing coefficient applied to g'_{fqa} to obtain g_{fqa}

e = per unit armature terminal voltage

E = particular value of per unit armature terminal voltage

e_d = per unit direct-axis field excitation, neglecting saturation

E_d = particular value of per unit direct-axis field excitation, neglecting saturation

F = field ampere turns in the air gap required to produce normal voltage in the armature on open circuit

$g_{\min}$ = iron-to-iron air gap at the pole center when opposite an armature tooth

g'_{fdf} = apparent air gap seen by the maximum ordinate of the fundamental direct-axis field flux-density wave, neglecting fringing phenomena

g_{fdf} = air gap seen by the maximum ordinate of the fundamental direct-axis field flux-density wave including fringing phenomena

g'_{fqa} = apparent air gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux-density wave, neglecting fringing phenomena

g_{fqa} = air gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux-density wave, including fringing phenomena

I_1 = particular value of per unit line current

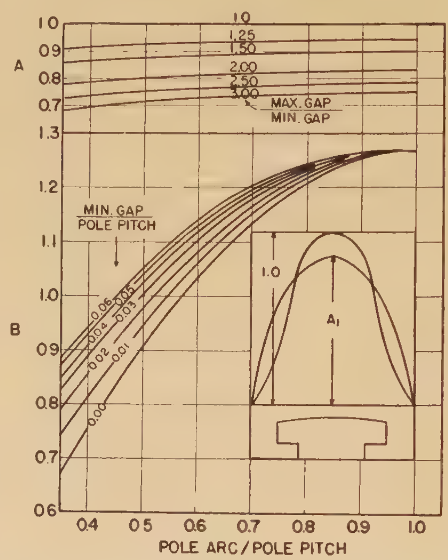

Figure 1. Ratio of the maximum amplitudes of the fundamental component and the actual flux-density waves in the air gap of a synchronous machine excited only from the rotor

Maximum value of actual flux-density wave = unity

Maximum value of fundamental flux-density wave, $A_1 = A \times B$

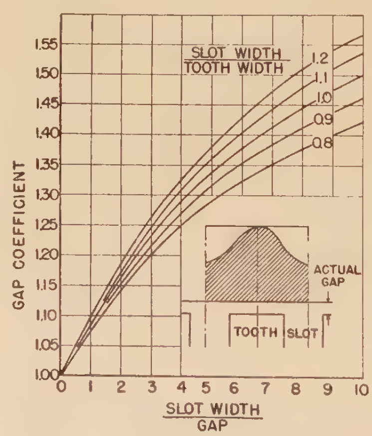

Figure 2. Air-gap fringing coefficient for the flux around a tooth of finite width and a slot of infinite depth as obtained by elliptic function analysis

Use the magnetic gap in place of the actual gap when the two differ

P = per unit value of shaft power (unit power is numerically equal to base kilovolt-amperes)

P_1 = particular value of per unit shaft power

P_r = synchronizing coefficient determined by dividing the shaft power in kilowatts by the corresponding angular displacement in electrical radians of the rotor from its zero shaft power position

Q = per unit value of reactive power (Unit reactive power is base kilovolt-amperes)

Q_1 = particular value of per unit reactive power

r_a = per unit armature resistance

x_l = per unit armature leakage reactance

x_d = per unit direct-axis synchronous reactance

x_q = per unit quadrature-axis synchronous reactance

x_{aq} = per unit quadrature-axis reactance of armature reaction

x = per unit external reactance

δ = load angle in electrical degrees

Γ, Υ, Ω = quantities defined in equations 20, 22 and 25, respectively

Appendix B. The Calculation of x_q

The quadrature-axis synchronous reactance x_q is defined as the ratio of the fundamental component of reactive armature voltage, due to the fundamental quadrature-axis component of armature current to this component of current under steady-state conditions and at rated frequency. Usually the reactance is taken as the value corresponding to rated armature current.

Alger[7] divided this quantity into two parts, the reactance of armature reaction

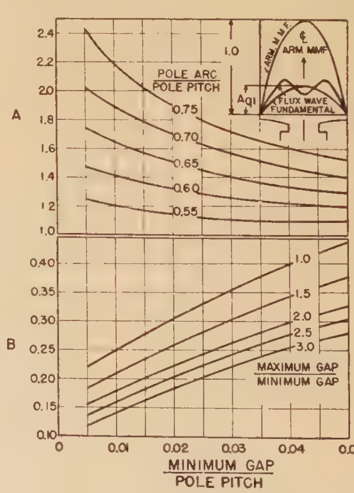

Figure 3. Fundamental of the air-gap flux-density wave when a salient-pole synchronous machine is excited by only a sine-wave armature magnetomotive force whose axis is in quadrature with the pole center

Maximum value of armature magnetomotive force = unity. Fundamental $A_{q1} = A \times B$

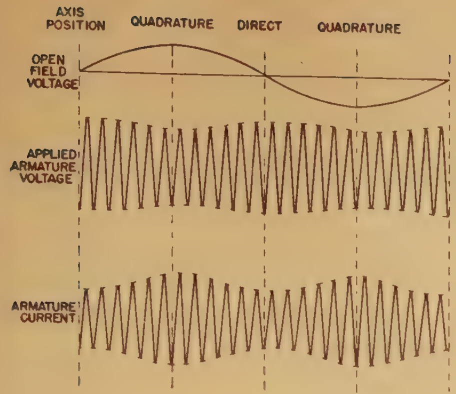

Figure 4. Appearance of oscillogram, slip test for x_q

In practice slip must be the minimum possible and only a small fraction of that illustrated

$$x_q = \frac{\text{per unit applied armature voltage}}{\text{per unit armature current}}$$

where both values are read from the quadrature-axis position

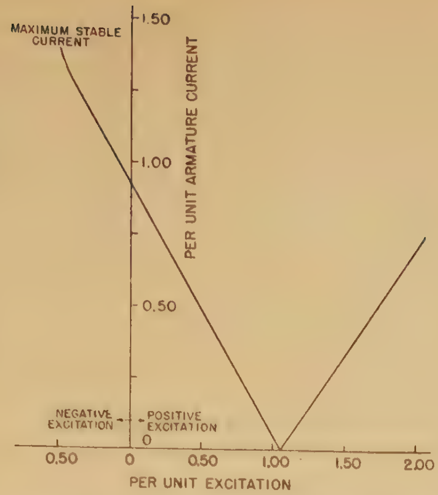

Figure 5. Per unit armature current versus excitation at constant terminal voltage

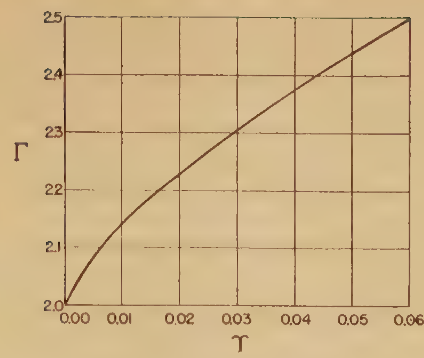

Figure 6. Plot of equation 22

x_{aq}, and the armature leakage reactance x_l, and produced methods for the calculation of x_l. Wieseman,[9] by means of curves derived from flux plots, evaluated coefficients A_1 and A_{q1} which, for smooth air gaps, made possible the determination of the ratio of fundamental air-gap flux due to field magnetomotive force. Linville[4] showed how to use these coefficients to calculate x_{aq}. Both Wieseman's presentation and Linville's employment of it were, as noted, based on smooth gaps. Kilgore[2] pointed out that an "effective" gap, not the actual measured gap at the pole center, must be used when slotted surfaces are considered, but a method of obtaining that effective gap and the employment of the result has not been presented. This, a step needed for the accurate calculation of x_q, is presented here.

The problem can be approached most clearly by first considering the phenomena existing in the direct-axis air gap when the machine is excited only by the field winding. Next, the effect of quadrature excitation from the armature will be introduced, and finally the two effects will be considered jointly to produce an equation for x_{aq}.

Assume, for the moment, that sufficient field excitation is applied to a smooth air gap to produce an actual flux-density wave whose maximum ordinate, at the pole center, is unity. Then from Figure 1, the amplitude of the maximum ordinate of the fundamental of that wave is A_1. The air gap seen by the maximum ordinate of the actual flux-density wave is the iron-to-iron gap at the pole center $g_{\min}$. Since both the actual and fundamental flux-density waves are created by the same magnetomotive force, their different amplitudes indicate that the magnetic gaps seen by the actual and fundamental components are different. Since the ratio of the amplitude of the flux-density waves is A_1, and the gap seen by the maximum ordinate of the actual wave is $g_{\min}$, then the gap seen by the maximum ordinate of the fundamental wave is

$$g'_{fdf} = \frac{g_{\min}}{A_1} \tag{1}$$

Carter has derived the relationship between the magnetic gaps of two parallel smooth surfaces and one smooth and one slotted surface. His fringing coefficients, which also can be thought of as "equivalent gap ratios" are well known. Figure 2 shows this coefficient for stator slots over a range of parameters encountered in synchronous machines. Similar characteristics can be obtained for stator air ducts, rotor slots, and other discontinuities in the magnetic surfaces. When these fringing coefficients are applied to the fundamental gap, g'_{fdf}, the relationship between the measured iron-to-iron minimum gap and the magnetic minimum gap for the fundamental component of the actual flux wave can be approximated closely.

To use this approximation, the actual iron-to-iron minimum gap, $g_{\min}$, is the starting point. g'_{fdf}, the effective smooth gap for the direct-axis fundamental flux-density wave due to rotor excitation, is next found from equation 1. Then using g'_{fdf}, the fringing coefficient for the armature slots is found from Figure 2, and similarly, fringing coefficients for other magnetic discontinuities in the air gap are obtained. If the product of the several fringing coefficients for this condition is $C_{g_{fdf}}$, then the magnetic gap seen by the fundamental component of the direct-axis field-excited flux-density wave is

$$g_{fdf} = C_{g_{fdf}} g'_{fdf}$$

$$= C_{g_{fdf}} \frac{g_{\min}}{A_1} \tag{2}$$

Figure 3 shows the ratio between the maximum per unit amplitudes of the fundamental component of the flux-density wave and the fundamental magnetomotive-force wave in the quadrature axis when the machine is excited only by a sine wave of armature magnetomotive force in the quadrature axis. If this identical unit magnetomotive-force wave that is applied to the complex smooth gap configuration of Figure 3 were applied to a constant-length smooth gap equal to $g_{\min}$, then the fundamental component of the flux-density wave would be unity. Therefore, the ratio A_{q1} is not only a ratio between a magnetomotive-force wave and a flux-density wave, but may also be thought of as the ratio between the fundamental component of quadrature-axis armature flux and unit fundamental armature flux, both of which are created by unit fundamental armature magnetomotive force. Hence, like A_1, it also is an inverse ratio of magnetic gap and the gap seen by the maximum ordinate of the fundamental quadrature-axis armature flux is

$$g'_{fqa} = \frac{g_{\min}}{A_{q1}} \tag{3}$$

If similar approximations are applied to g'_{fqa} as were applied to g'_{fdf} to get the appropriate fringing coefficients, the magnetic gap seen by the fundamental component of quadrature axis armature flux is

$$g_{fqa} = C_{g_{fqa}} g'_{fqa}$$

$$= C_{g_{fqa}} \frac{g_{\min}}{A_{q1}} \tag{4}$$

Under the assumption of smooth air-gap surfaces and with the application of identical fringing coefficients to all methods of excitation, the relationships determining x_{aq} have been shown to be[4]

$$x_{aq} = \frac{4}{\pi} \frac{A}{F} \frac{A_{q1}}{A_1} \tag{5}$$

When the fringing coefficients are recognized to be different and dependent upon the method of excitation, as set forth here, this equation becomes

$$x_{aq} = \frac{4}{\pi} \frac{A}{F} \frac{A_{q1}}{A_1} \frac{C_{g_{fdf}}}{C_{g_{fqa}}} \tag{6}$$

Also there is the relationship

$$x_q = x_l + x_{aq} \tag{7}$$

Therefore

$$x_q = x_l + \frac{4}{\pi} \frac{A}{F} \frac{A_{q1}}{A_1} \frac{C_{g_{fdf}}}{C_{g_{fqa}}} \tag{8}$$

Appendix C. The Slip Test For x_q

The slip test for x_q has already been presented adequately in the technical press,[6] and so only a brief description is necessary. The machine to be tested is either coupled to a second machine or run with a free shaft and positive-sequence voltage applied to the terminals. The field winding is open-circuited. The terminal voltage or shaft torque is then adjusted so that the reluctance

torque of the machine is overcome, causing the field to slip past the armature magnetomotive-force wave at a low value of slip. An oscillographic record of the armature current, terminal voltage, and induced field voltage, is then taken. Figure 4 illustrates the appearance of such an oscillogram.

The quadrature synchronous reactance equals the minimum ratio of the per unit armature voltage and current read from the oscillogram. The correct values of armature voltage and current are obtained at those instances when the induced field voltage is at a positive or negative maximum.

The slip method of measurement, using an oscillograph, has the advantage of measuring most clearly those quantities on which the definition of x_q is based and of providing a permanent record of the quantities read. It also has disadvantages. Since the terminal voltage applied must usually be approximately 20 per cent to 30 per cent of the rated voltage and must be adjusted accurately, a controllable low-voltage power supply is required. It is important that the slip be very small; otherwise in machines having amortisseur windings, currents will be induced in these windings, and the reactances read from the oscillogram will be low. Even under the most strictly controlled conditions the instantaneous slip of the rotor is usually a minimum when the stator magnetomotive force is passing through the quadrature axis, and hence a difference between the accuracies of x_d and x_q, as read by the oscillogram, is inherent in the method.

For these reasons the slip method is generally limited to testing in the manufacturer's plant and is not broadly suitable for field work.

Appendix D. Reluctance Torque Test for x_q

Kingsley[1] first suggested the reluctance-torque test for x_q. Neglecting losses, the power-angle equation of an ideal synchronous machine connected to an infinite bus is given by[10]

$$P=\frac{ee_d}{x_d}\sin\delta+\frac{e^2(x_d-x_q)}{2x_dx_q}\sin 2\delta \qquad (9)$$

If the machine is operated as a reluctance motor, $e_d=0$, and maximum synchronous power will occur when $\sin 2\delta=1.0$ (that is, $\delta=45$ degrees). Any change tending to increase δ will cause loss of synchronism.

By lowering the terminal voltage or increasing the shaft load, the machine may be made to pull out of synchronism. Knowing x_d from other tests, and determining E and P_1 at the point at which loss of synchronism occurs, x_q can be found from the relation

$$x_q=\frac{E^2x_d}{E^2+2P_1x_d} \qquad (10)$$

It can be shown that the effect of armature resistance is approximately taken into account by the equation

$$x_q=\frac{E^2(x_d+2r_a)}{E^2+2P_1x_d} \qquad (11)$$

If P_1 is only the friction and windage of the machine, the drop-out voltage will be low (15 per cent to 25 per cent of normal), saturation will not be present, and core losses will be small.

Errors in the measurement of the power, and particularly the pull-out voltage, are possible as the machine is unstable as this condition is approached. The magnitude of error in determining x_q can be found from the following equation, which is derived from equation 10:

$$\frac{dx_q}{x_q}=\left(1-\frac{x_q}{x_d}\right)\left(2\frac{de}{e}-\frac{dP}{P}\right) \qquad (12)$$

Reactance external to the test machine causes pull-out at some angle less than 45 degrees, and the calculated value of x_q is too high.

If x is the external reactance, the load angle at pull-out is found to be

$$\delta=\text{arc tan}\,\frac{x_q\,(x_d+x)}{x_d\,(x_q+x)} \qquad (13)$$

The value of x_q is then given by

$$x_q=\frac{E^2(x_d\sin 2\delta+2r_a)}{E^2\sin 2\delta+2P_1x_d} \qquad (14)$$

If the effects of armature resistance and external reactance are neglected, the error is given, very closely, by the relation

$$\frac{\Delta x_q}{x_q}\cong\left(1-\frac{x_q}{x_d}\right)(1-\sin 2\delta) \qquad (15)$$

The reluctance-torque test for x_q has one advantage over the slip test, namely, that satisfactory readings are possible without the use of an oscillograph. It has many of the disadvantages of the slip test. Again a controllable low-voltage source is usually required, and tests are generally possible only on the manufacturer's premises. In addition, shaft power must be known accurately.

Appendix E. Negative-Excitation Test for x_q

The negative-excitation test for x_q derives its name from the reversed polarity which is imposed on the field winding when the instruments are read and the necessity of recording this value. The machine to be tested is run as a synchronous motor, with a free shaft and with constant applied rated terminal voltage. The field current is initially adjusted to its value at the low point in the V-curve characteristic. It is then decreased to zero, causing an increase in line current, and, after reaching zero field current, the polarity of the applied excitation is reversed and field current increased in the negative direction, causing a continuing increase in the armature current. See Figure 5. By applying small increments in field current as the maximum stable negative field current is approached, the highest per unit value E_d of field current that can be obtained is found. Per unit shaft power, P_1, at this point is also determined. If the per unit direct-axis synchronous reactance x_d is known from other tests, x_q can then be found from the two readings taken.

Neglecting losses and applying negative

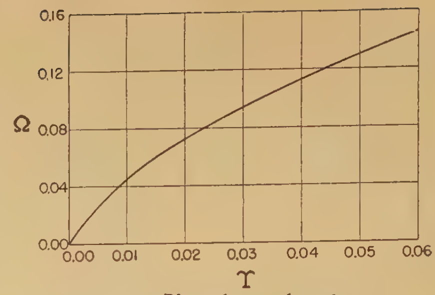

Figure 7. Plot of error function Ω

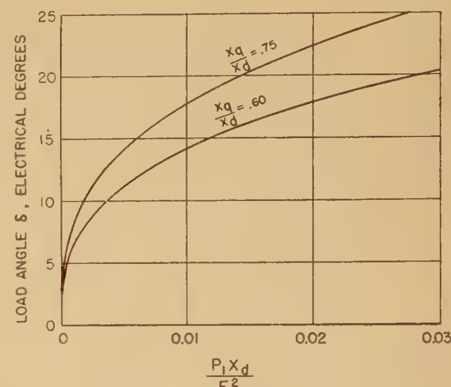

Figure 8. Plot of equation 31

excitation, the power-angle equation 9 becomes

$$P=\frac{e(-e_d)}{x_d}\sin\delta+\frac{e^2(x_d-x_q)}{2x_dx_q}\sin 2\delta \qquad (16)$$

When the machine is operating at its maximum stable point, there is

$$\frac{dP}{d\delta}=0 \qquad (17)$$

and

$$\frac{ee_d}{x_d}\cos\delta=\frac{e^2(x_d-x_q)}{x_dx_q}\cos 2\delta \qquad (18)$$

from which

$$\delta=\text{arc cos}\,\frac{1.0+\sqrt{1+2\Gamma^2}}{2\Gamma} \qquad (19)$$

where

$$\Gamma=\frac{2e}{e_d}\left(\frac{x_d}{x_q}-1.0\right) \qquad (20)$$

Combining and simplifying equations 16, 19 and 20 there results

$$P=\frac{ee_d}{x_d}\Upsilon \qquad (21)$$

where

$$\Upsilon=\sqrt{\frac{(\sqrt{1.0+2\Gamma^2}-3.0)^3}{32(\sqrt{1.0+2\Gamma^2}-1.0)}} \qquad (22)$$

Equation 22 is plotted in Figure 6.

If test values of $P(P=P_1)$, $e_d(e_d=E_d)$, $e(e=E)$, and x_d are substituted in equation 21, there is

$$\Upsilon=\frac{P_1x_d}{EE_d}$$

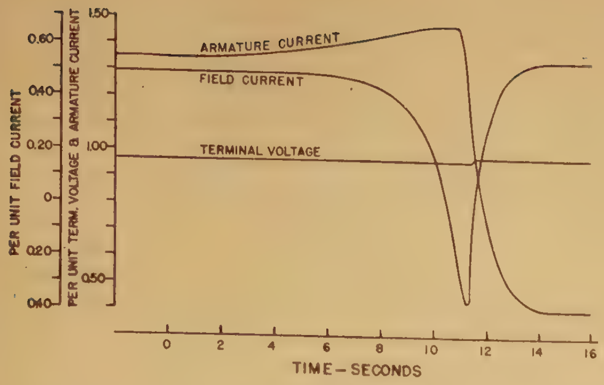

Figure 9. Terminal characteristics during a negative-excitation slip cycle

and the quantity Υ can be evaluated by test. Figure 6 can then be used to evaluate Γ, and from equation 20

$$x_q=\frac{x_d}{\frac{\Gamma E_d}{2E}+1.0} \tag{23}$$

Test errors may occur in the determination of E_d and P_1. It can be shown that, if such errors do occur, the effect on x_q can be evaluated from the relationship

$$\frac{dx_q}{x_q}=-\left(1.0-\frac{x_q}{x_d}\right)\left(\Omega\frac{dP_1}{P_1}+(1.0-\Omega)\frac{dE_d}{E_d}\right) \tag{24}$$

where

$$\Omega=\frac{\sqrt{1.0+2\Gamma^2}-3.0}{\sqrt{1.0+2\Gamma^2}+1.0} \tag{25}$$

The error function Ω is plotted against Υ in Figure 7 for the range of Υ usually encountered. A study of this reveals that the value of x_q so determined is more affected by a test error in E_d than by a corresponding one in P_1.

The negative-excitation test for x_q, has an important advantage over both the slip and reluctance-torque methods of test in that a controllable low-line-voltage source is not required. Tests can be made on the user's premises where rated line voltage is frequently the only source of power, but, as in the reluctance-torque test, shaft power must be known, and this introduces the problem of measuring it.

Appendix F. Maximum-Lagging-Current Test for x_q

The maximum-lagging-current test for x_q is made in exactly the same manner as the negative-excitation test discussed in Appendix E. With the machine operating with a free shaft and with constant terminal voltage applied, the maximum stable per unit line current I_1, corresponding to the maximum stable negative excitation E_d, is found. x_q can then be found from these line-voltage and line-current values.

Neglecting losses and applying negative excitation as in Appendix E, the magnitude of negative excitation at the maximum stable operating point is, by solving equation 18, found to be

$$e_d=\frac{e(x_d-x_q)}{x_q}\,\frac{\cos 2\delta}{\cos\delta} \tag{26}$$

The general reactive-power-angle equation of a synchronous machine connected to an infinite bus is

$$Q=\frac{ee_d}{x_d}\cos\delta-\frac{e^2}{x_d}\cos^2\delta-\frac{e^2}{x_q}\sin^2\delta \tag{27}$$

When negative excitation, as given in equation 26, is applied to equation 27, the maximum stable reactive power is

$$Q_1=-\frac{e^2}{x_q}\left[1-\left(1-\frac{x_q}{x_d}\right)\sin^2\delta\right] \tag{28}$$

In an ideal machine operating at no load with no losses, the load angle δ and the active power P are both zero when maximum reactive power is reached.

For these conditions

$$Q_1=EI_1=-\frac{E^2}{x_q} \tag{29}$$

and in magnitude

$$x_q=\frac{E}{I_1} \tag{30}$$

In an actual machine, power must be developed to overcome friction and windage, and the load angle at the maximum stable point cannot be zero. However, neglecting armature losses, it can be shown as follows that for the usual range of machine constants and friction and windage losses, these effects produce negligible errors in equation 30.

If equation 26 is substituted in equation 9, the power P_1 with negative excitation and at the maximum stable point is

$$P_1=\frac{E^2(x_d-x_q)}{x_dx_q}\sin^2\delta\tan\delta \tag{31}$$

Figure 8 is a plot of equation 31 showing the relationship between δ and P_1x_d/E^2 over a range of machine constants. It can be seen from Figure 8 that at normal voltage and with usual values of x_d and P_1, δ may be as large as 20 degrees but probably will not exceed 15 degrees. If $\delta=20$ degrees, and $x_q/x_d=0.6$, equation 28 gives a value for Q_1 which is 95.7 per cent of the maximum possible value given by equation 29. Hence, x_q, as determined by equation 30, would be approximately five per cent high. The inclusion of armature losses will decrease this figure slightly. It is felt that this figure of five per cent is a reasonable maximum variation between x_q determined by this method and the correct value and that in most cases the error will be less than five per cent. Table I justifies this conclusion.

The terminal characteristics during a typical negative-excitation slip cycle have been recorded on oscillograms. Figure 9 shows the envelopes of the terminal voltage and armature current and a trace of the field current during one of these cycles. Previous to time $t=0$, the machine was operating at the maximum-stable-armature-current point of Figure 5. At $t=0$ the field voltage was slightly increased, and the machine became unstable and started to slip. As the slip increased, the armature current increased, reaching a maximum unstable value at $t=11$ seconds. As the unstable region was passed, the armature current fell until at $t=16$ seconds it was operating stably on the positive excitation side of the V curve of Figure 5. This phenomenon of rise and fall in current, when passing through the slip cycle, is readily apparent on test meters. The maximum stable current, not the maximum current during slipping, is used to determine x_q.

The maximum-lagging-current test for x_q has a great advantage over all other test methods in that the instruments required are perfectly standard and widely available. The test is made at rated voltage, and shaft power is not required to calculate the results. It is by far the simplest of the methods presented.

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Practical Calculation of Electrical Transients on Power Systems

R. D. EVANS
FELLOW AIEE

R. L. WITZKE
ASSOCIATE AIEE

Synopsis: Solution of many power-circuit problems depends upon the magnitude, duration, and frequency of occurrence of transient voltages and currents produced by circuit changes. Circuit-opening or recovery-voltage transients are fundamental in the operation of interrupting devices and may be important from the standpoint of the dielectric strength of circuit elements. Circuit-closing transients may be important from the standpoint of inrush currents and occasionally from the standpoint of overvoltages. Both types of transients may be increased greatly if restriking and repetitive interruptions take place.

Transient voltages and currents resulting from circuit changes can be calculated by the classical method with the aid of differential equations or by the Heaviside method, particularly with the aid of the expansion theorem. Transients on complicated power systems are most conveniently determined by the miniature-system method introduced by Evans and Monteith and described in their 1937 AIEE paper.[4] Analytical methods are useful for simple problems and are essential for those beyond the range of the a-c network calculator or when it is not available. Analytical methods are also necessary for determining the proper form of the equivalent networks for the representation of power systems on the a-c network calculator. This circumstance provided the occasion for the particular investigation for which the present paper is a partial report.

This paper is devoted to the practical calculation of power-system transients and presents concepts, methods, and approximations selected with that objective in view. The paper describes a method of calculation particularly adapted for engineering work.

The scope of the present paper is limited to networks consisting of constant inductance and constant capacitance elements without loss. Consideration of such networks facilitates the presentation of the subject, and gives an adequate view of the phenomena involved. The presentation is from the standpoint of the electrical engineer who is accustomed to making steady-state calculations but who has not given the special study to the transient problem required by the mathematical approach generally used.

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R. D. Evans and R. L. Witzke are, respectively, consulting transmission engineer and central station engineer for Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

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The classical method of determining the transients produced by the application of voltage or current to a network is carried out by setting up a set of linear differential equations. The general solution of the set of equations for the case of a suddenly applied voltage may be written as follows:

$$i(t)=A_0(t)+A_1 \sin \omega_1 t+A_2 \sin \omega_2 t \ldots A_n \sin \omega_n t \quad (1)$$

In this equation, $\omega_1, \omega_2 \ldots \omega_n$ are the modes of oscillation of the network ($2\pi \times$ natural frequencies); $A_1, A_2, \ldots A_n$ are the constants that define the amplitudes of the corresponding modes; and $A_o(t)$ is the term that defines the steady-state current and the nonoscillatory components of the transient current. The integration constants $A_1 \ldots A_n$ can be determined from the boundary conditions in the usual manner. A corresponding equation may be written for the voltages produced by the application of current to a network.

The general solution of the transient problems in networks is readily accomplished in a formal manner, but considerable work remains before the numerical answers are obtained. Determination of the modes of oscillation, although required by all methods (at least for principal terms), involves considerable labor when the equations are of a high degree. Determination of the integration constants, while straightforward from a mathematical point of view, frequently offers considerable difficulty to engineers not accustomed to transient calculations. Because of the limitations of the classical method, the analytical work presented in this paper has been based on the Heaviside expansion theorem. Before taking up the method of analysis described in this paper, it is desirable to discuss in some detail the general properties of no-loss networks.

Properties of Networks

The reactance and susceptance characteristics of networks are fundamental to the method of analysis described in this paper and so will be discussed first. The reactance properties are used when the method is employed in solving circuit-closing problems, whereas the susceptance properties are used in circuit-opening problems. Accordingly, the discussion of networks is arranged in two parts, one for circuit-closing and the other for circuit-opening problems.

All no-loss two-terminal networks consisting of fixed circuit elements are of four types, depending only on the reactances at zero and at infinite frequencies. The principal characteristics of these four types of networks are given in Table I. The network can be typed readily by considering the character of the paths between the driving-point terminals as shown in the table. For example, type I networks have no paths between terminals containing inductance or capacitance alone, that is, all paths contain both elements. Representative networks of the four types are illustrated in the table. Angular velocity-reactance curves corresponding to the representative networks are also given in Table I. These curves are obtained by calculating the steady-state reactance as a function of angular velocity in the usual manner.

The network-reactance curves are discontinuous functions that start from negative infinity and increase to zero or to positive infinity or that start at zero and increase to positive infinity. In other words, all reactance curves have positive slopes[2] throughout their continuous portions. The infinite-reactance points or points of discontinuity are termed "poles," and the zero-reactance points are termed "zeros." These definitions are in accordance with accepted terminology and are used in the subsequent discussion of network characteristics. The "zeros" and "poles" at zero and at infinite frequencies are called "external zeros" and "external poles" and the remaining ones are called "internal zeros" and "internal poles." As can be seen readily from the reactance curves of Table I, the external zeros and poles also define the network type; for example, the type I network has poles at zero and at infinite frequencies. The number of internal zeros that a network has is equal to the number of modes of oscillation; this number is also equal to the maximum number of meshes containing both independent inductive and capacitive elements.

Basic equivalent networks in a form convenient for circuit-closing transients are also included in Table I for each network type. In the basic networks there is one branch containing both L and C for each internal zero; in addition, there are other branches containing only L or only C as required to correspond to the

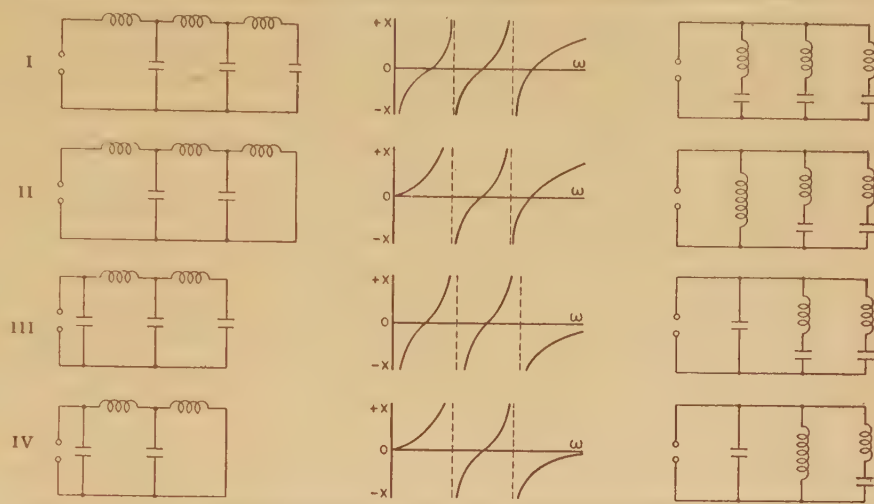
external zeros. A branch containing L alone is required when the network has a zero at zero frequency and a branch containing C alone is required when the network has a zero at infinite frequency. Thus a network of type IV, having zeros at zero and at infinite frequencies, requires one branch with L alone and one branch with C alone.

All no-loss two-terminal networks can also be classified according to the network susceptance at zero and infinite frequencies, as shown in Table II. The representative networks in Table II are identical to the corresponding types in Table I. The angular velocity-susceptance curves are included in the table for each network type. Basic equivalent networks of the form convenient for circuit-opening transients are also included in Table II.

Table I. Classification of Networks for Circuit-Closing Transients

Type	X at $\omega=\infty$	X at $\omega=0$	Paths: C Alone	Paths: L Alone
I	∞	∞	None	None
II	∞	0	None	At least one
III	0	∞	At least one	None
IV	0	0	At least one	At least one

Typical Networks — Typical Reactance Curves — Basic Equivalent Networks



Method of Zeros and Slopes

As the method of analysis to be described depends upon the zeros and slopes of the reactance (or susceptance) curves for the networks, it is referred to as the method of "zeros and slopes." The method is described under two headings:

(*a*). Unit voltage applied.
(*b*). Unit current applied.

The method will be extended for the application of a-c voltages and currents in a later section.

(a). Unit Voltage Applied

The response of a network to a suddenly applied unit voltage is given by the Heaviside expansion theorem as follows:

$$i(t)=i_o(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)} \tag{2}$$

In this form of the expansion theorem, the function $i_o(t)$ defines the steady-state network solution and that part of the transient solution determined by the external zeros. The term $Z'(p)$ is the first derivative of $Z(p)$ with respect to p, where $Z(p)$ is the impedance function of the network as viewed from the driving point. The summation indicates that there are as many terms in the transient solution (neglecting the $i_o(t)$ term) as there are roots of the equation $Z(p)=0$. The quantity p_k is a general expression for the roots of $Z(p)=0$.

It is shown in the appendix that when restricted to no-loss networks, equation 2 can be written in the following approximate form:

$$i(t)=i_o(t)+A_1\sin\omega_1t+A_2\sin\omega_2t+\ldots+A_n\sin\omega_nt \tag{3}$$

where

$$A_1=\frac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}};\quad A_2=\frac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}};\quad\ldots A_n=\frac{2}{\omega_n\dfrac{\Delta X}{\Delta\omega_n}}$$

The $i_o(t)$ term defines the steady-state current and the components of the transient current corresponding to the external zeros. This term is given in Table III for each type of network. The term t/L_o defines a current starting at zero and increasing directly with time to an infinite value at $t=\infty$. The term $\left[\dfrac{t}{C_o}\right]_{t=0}$ defines a current having an infinite magnitude at $t=0$ and equal to zero thereafter, such as instantly to bring the charge on the capacitance to the applied voltage. The quantity L_o is defined as the equivalent inductance between driving-point terminals with all capacitances open-circuited, and C_o is the equivalent capacitance with all inductances open-circuited.

In equation 3, ω_1, ω_2, … ω_n are the values of angular velocity corresponding to zero reactance between the driving-point terminals, and $\dfrac{\Delta X}{\Delta\omega_1}$, $\dfrac{\Delta X}{\Delta\omega_2}$, … $\dfrac{\Delta X}{\Delta\omega_n}$ are the slopes of the angular velocity-reactance curve at the corresponding values of angular velocity. This relation shows that the response of a no-loss network can be determined from the steady-state reactance characteristics of the network. An approximate solution can, therefore, be obtained by calculating the angular velocity-reactance curve and then determining the slopes of the curve at or near the zeros. This procedure is illustrated in Figure 1 for a relatively simple circuit. It should be noted that it is not necessary to obtain the complete curve but only those parts in the vicinity of the zeros. The number of points that must be calculated will depend upon the accuracy desired and on the ability of the operator to estimate the zeros. The accuracy of the solution will depend entirely on the accuracy with which the zeros and slopes are evaluated.

(b). Unit Current Applied

The problem of calculating the transient voltage following the opening of a circuit can be analyzed in terms of the original current and a suddenly applied equal and opposite current.* The superposition of these two currents reduces the total current to zero, which is equivalent to opening the circuit. Thus, the first step is to calculate the network response to unit current.

The response of a network to a suddenly applied unit current can also be written in a form similar to equation 3.

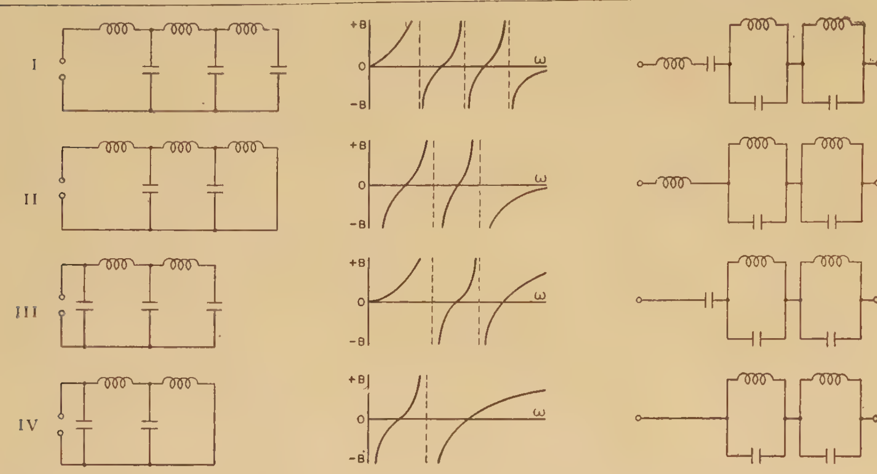
$$e(t)=e_o(t)+A_a\sin\omega_at+A_b\sin\omega_bt+\ldots+A_m\sin\omega_mt \tag{4}$$

* Page 306 of reference 1.

Table II. Classification of Networks for Circuit-Opening Transients

Type	B at $\omega=\infty$	B at $\omega=0$	Paths: C Alone	Paths: L Alone
I	0	0	None	None
II	0	∞	None	At least one
III	∞	0	At least one	None
IV	∞	∞	At least one	At least one

Typical Networks	Typical Susceptance Curves	Basic Equivalent Networks



where

$$A_a=\frac{2}{\omega_a\dfrac{\Delta B}{\Delta\omega_a}};\ A_b=\frac{2}{\omega_b\dfrac{\Delta B}{\Delta\omega_b}};\ \ldots A_m=\frac{2}{\omega_m\dfrac{\Delta B}{\Delta\omega_m}}$$

The $e_o(t)$ term defines the steady-state voltage and the components of the transient voltage corresponding to the external zeros. This term is given in Table III for each type of network. The term t/C_s defines a voltage starting at zero and increasing directly with time to an infinite value at $t=\infty$. The term $\left[\dfrac{L_s}{t}\right]_{t=0}$ defines a voltage having an infinite magnitude at $t=0$ and equal to zero thereafter. The quantity C_s is the equivalent capacitance between driving-point terminals with all inductances short-circuited, and L_s is the equivalent inductance with all capacitances short-circuited.

In equation (4), $\omega_a, \omega_b, \ldots \omega_m$ are the values of angular velocity resulting in zero susceptance between driving-point terminals, and $\dfrac{\Delta B}{\Delta\omega_a}, \dfrac{\Delta B}{\Delta\omega_b}, \ldots \dfrac{\Delta B}{\Delta\omega_m}$ are the slopes of the angular velocity-susceptance curve at the corresponding points. The response of a no-loss network to unit current can, therefore, be obtained in a manner similar to that illustrated for the response to unit voltage. However, in this case the steady-state susceptance characteristics of the network are used instead of the reactance characteristics.

Table III

Network Type	$i_0(t)$	$e_0(t)$
I	0	$\left[\frac{L_s}{t}\right]_{t=0}+\frac{t}{C_s}$
II	$\frac{t}{L_o}$	$\left[\frac{L_s}{t}\right]_{t=0}$
III	$\left[\frac{C_o}{t}\right]_{t=0}$	$\frac{t}{C_s}$
IV	$\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}$	0

L_s = Equivalent L determined with all C's short-circuited
L_o = Equivalent L determined with all C's open-circuited
C_o = Equivalent C determined with all L's open-circuited
C_s = Equivalent C determined with all L's short-circuited

Equivalent Networks

Every network can be reduced to an equivalent network[2,3] of either the shunt or series forms, illustrated in Figure 2. The basic equivalent network of the shunt form is advantageous for circuit-closing transients because of the lack of coupling between branches for this type of circuit change. Similarly, the basic equivalent network of the series form is advantageous for circuit-opening transients. These equivalent networks are helpful in visualizing the properties of networks and in interpreting the equations of transient response. Furthermore, the replacement of a given complicated network by the simplest equivalent network of a given form is advantageous under many circumstances for calculation by analytical or a-c network calculator methods.[4]

(a). Circuit-Closing Transients

The basic equivalent network in the shunt form of Table I can be derived from the network response to unit voltage as given by equation 3. For convenience this equation has been rewritten with the $i_o(t)$ term expanded as given in Table III:

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+A_1\sin\omega_1 t+ A_2\sin\omega_2 t\ldots A_n\sin\omega_n t \quad \text{(5a)}$$

$$=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\frac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}}\sin\omega_1 t+ \frac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}}\sin\omega_2 t\ldots\frac{2}{\omega_n\dfrac{\Delta X}{\Delta\omega_n}}\sin\omega_n t \quad \text{(5b)}$$

The basic equivalent network of the shunt form is illustrated in Figure 2*a*. Each branch of this equivalent network corresponds to one term of the network solution given by equation 5. The L_o and C_o branches correspond, respectively, to an equivalent inductance or an equivalent capacitance in parallel with the driving-point terminals, as defined in Table III. As the response of a simple series LC circuit to unit voltage is $\sqrt{\dfrac{C}{L}}\sin\omega t$, where $\omega=\dfrac{1}{\sqrt{LC}}$, equation 5 can be written in the following form:

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\sqrt{\frac{C_1}{L_1}}\sin\omega_1 t+ \sqrt{\frac{C_2}{L_2}}\sin\omega_2 t\ldots\sqrt{\frac{C_n}{L_n}}\sin\omega_n t \quad (6)$$

where

$$\omega_1=\frac{1}{\sqrt{L_1C_1}};\ \omega_2=\frac{1}{\sqrt{L_2C_2}};\ \ldots\ \omega_n=\frac{1}{\sqrt{L_nC_n}}$$

By equating the coefficients of equations 5*a* and 6, the remaining circuit elements of the equivalent network can be determined as follows:

$$\left.\begin{aligned}
&A_1=\sqrt{\frac{C_1}{L_1}};\quad A_2=\sqrt{\frac{C_2}{L_2}};\quad\ldots\quad A_n=\sqrt{\frac{C_n}{L_n}}\\
&\omega_1=\frac{1}{\sqrt{L_1C_1}};\ \omega_2=\frac{1}{\sqrt{L_2C_2}};\ \ldots\omega_n=\frac{1}{\sqrt{L_nC_n}}\\
&C_1=\frac{A_1}{\omega_1};\quad C_2=\frac{A_2}{\omega_2};\quad\ldots\quad C_n=\frac{A_n}{\omega_n}\\
&L_1=\frac{1}{\omega_1A_1};\quad L_2=\frac{1}{\omega_2A_2};\quad\ldots\quad L_n=\frac{1}{\omega_nA_n}
\end{aligned}\right\} \quad (7)$$

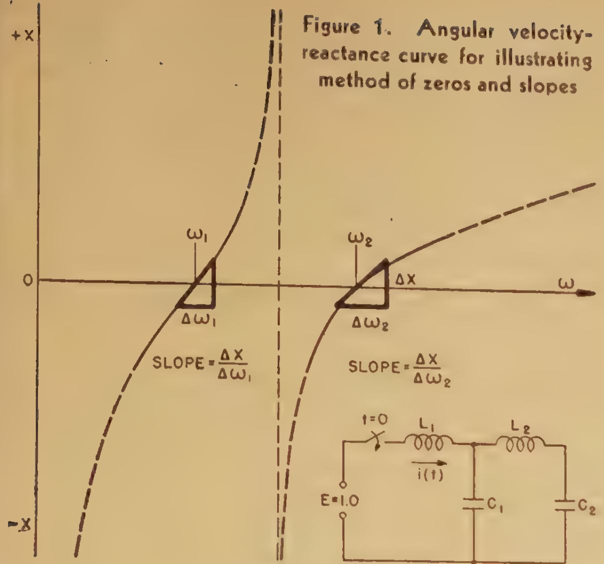
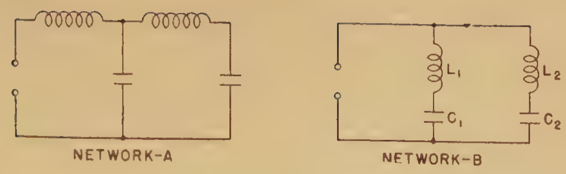


Figure 1. Angular velocity-reactance curve for illustrating method of zeros and slopes



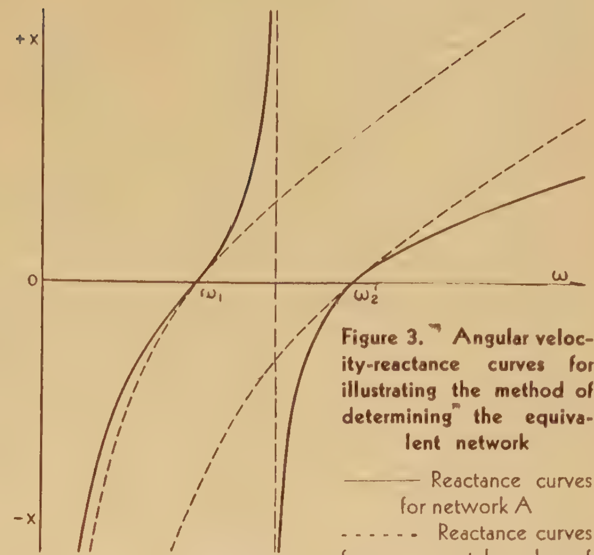


Figure 3. Angular velocity-reactance curves for illustrating the method of determining the equivalent network

——— Reactance curves for network A

- - - - - - Reactance curves for component branches of equivalent network B

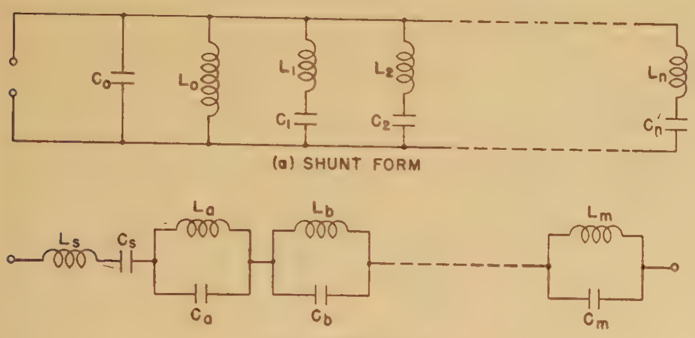


Figure 2 (left). Basic equivalent networks in the shunt and series forms

By equating the coefficients of equations 5b and 6, the circuit elements of the equivalent network can be stated directly in terms of the zeros and slopes:

$$\left.\begin{aligned}
&\sqrt{\frac{C_1}{L_1}}=\frac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}};\quad \sqrt{\frac{C_2}{L_2}}=\frac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}};\\
&\qquad\qquad\ldots\sqrt{\frac{C_n}{L_n}}=\frac{2}{\omega_n\dfrac{\Delta X}{\Delta\omega_n}}\\
&\omega_1=\frac{1}{\sqrt{L_1C_1}};\quad \omega_2=\frac{1}{\sqrt{L_2C_2}};\\
&\qquad\qquad\ldots\omega_n=\frac{1}{\sqrt{L_nC_n}}\\
&L_1=\frac{1}{2}\frac{\Delta X}{\Delta\omega_1};\quad L_2=\frac{1}{2}\frac{\Delta X}{\Delta\omega_2};\quad \ldots L_n=\frac{1}{2}\frac{\Delta X}{\Delta\omega_n}\\
&C_1=\frac{1}{\omega_1^2L_1};\quad C_2=\frac{1}{\omega_2^2L_2};\quad \ldots C_n=\frac{1}{\omega_n^2L_n}
\end{aligned}\right\} \tag{8}$$

These relations show that the circuit elements of the equivalent network can be determined directly from the angular velocity-reactance curves. This is graphically illustrated in Figure 3 for a simple two-mesh network. The reactance curves are shown for the original network and for each branch of the equivalent network. As the two networks are equivalent the reactance of network A is equal to the reactance of network B, which, of course, is equal to the parallel of the reactances of the individual branches. Each zero for the reactance of the total network occurs at the zero for a component branch and the corresponding slopes at these zeros are identical. This results from the fact that at the zero for one branch the reactance of the other branch becomes negligibly high in comparison with the branch under consideration. Thus, for each internal zero of the angular velocity-reactance curve there is an equivalent shunt branch containing L and C, the values of which are defined by the corresponding zero and slope. This same reasoning can be extended to any number of branches.

(b). Circuit-Opening Transients

The basic equivalent network in the series form of Table II can readily be derived from equation 4 for the response to unit current applied. For convenience this equation has been rewritten with the $e_o(t)$ term expanded as given in Table III:

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+A_a\sin\omega_at+A_b\sin\omega_bt\ldots+A_m\sin\omega_mt \tag{9a}$$

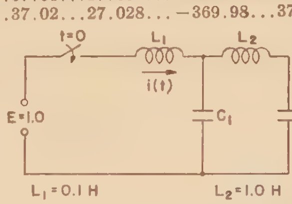
$$=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\frac{2}{\omega_a\dfrac{\Delta B}{\Delta\omega_a}}\sin\omega_at+\frac{2}{\omega_b\dfrac{\Delta B}{\Delta\omega_b}}\sin\omega_bt\ldots\frac{2}{\omega_m\dfrac{\Delta B}{\Delta\omega_m}}\sin\omega_mt \tag{9b}$$

Table IV. Unit Voltage Applied

(1) ω	(2) ωL_2	(3) $1/\omega C_2$	(4) (2)+(3)	(5) 1/(4)	(6) ωC_1	(7) (5)+(6)	(8) 1/(7)	(9) ωL_1	X (8)+(9)
				$x10^{-4}$	$x10^{-4}$	$x10^{1}$			
2,701	2,701	−3,702.3	−1,001.3	9.987	27.01	36.997	−270.29	270.1	−0.19
2,702	2,702	−3,700.9	−998.9	10.011	27.02	37.031	−270.04	270.2	+0.16
3,701	3,701	−2,701.9	−999.1	10.009	37.01	27.001	−370.35	370.1	−0.25
3,702	3,702	−2,701.2	−1,000.8	9.992	37.02	27.028	−369.98	370.2	+0.22

$\omega_1=2{,}701$
$\Delta\omega_1=1.0$
$\Delta X=0.19+0.16=0.35$
$\dfrac{\Delta X}{\Delta\omega_1}=0.35$
$\dfrac{2}{\omega_1\dfrac{\Delta X}{\Delta\omega_1}}=0.00212$

$\omega_2=3{,}701$
$\Delta\omega_2=1.0$
$\Delta X=0.47$
$\dfrac{\Delta X}{\Delta\omega_2}=0.47$
$\dfrac{2}{\omega_2\dfrac{\Delta X}{\Delta\omega_2}}=0.00115$



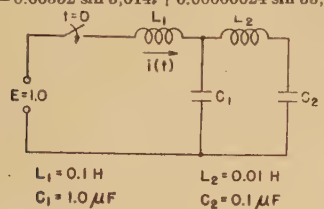
L1 = 0.1 H   L2 = 1.0 H
C1 = 1.0 μF   C2 = 0.1 μF

$i(t)=0.00212\sin 2{,}701t+0.00115\sin 3{,}701t$

Table V. Unit Voltage Applied

ω	X	$\frac{\Delta X}{\Delta\omega}$
3,013	−0.17	
3,014	+0.03	0.20
33,100	+4,073	
33,200	+1,969	
33,179	−191	
33,180	+61	252

$i(t)=0.00332 \sin 3{,}014t+0.00000024 \sin 33{,}179t$



The basic equivalent network of the series form is illustrated in Figure 2*b*. Each branch of this equivalent network corresponds to one term of the network solution given by equation 9. The L_s and C_s branches correspond to an equivalent inductance or an equivalent capacitance in series with the driving point, as defined in Table III. As the response of a simple parallel *LC* circuit to unit current is equal to $\sqrt{\frac{L}{C}} \sin \omega t$, where $\omega = \frac{1}{\sqrt{LC}}$, equation 9 can be written in the following form:

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\sqrt{\frac{L_a}{C_a}} \sin \omega_a t+\sqrt{\frac{L_b}{C_b}} \sin \omega_b t \ldots \sqrt{\frac{L_m}{C_m}} \sin \omega_m t \quad (10)$$

where

$$\omega_a=\frac{1}{\sqrt{L_aC}};\ \omega_b=\frac{1}{\sqrt{L_bC_b}};\ldots \omega_m=\frac{1}{\sqrt{L_mC_m}}$$

By equating the coefficients of equations 9*a* and 10, the remaining circuit elements of the equivalent network can be determined as follows:

$$\left.\begin{aligned}
&A_a=\sqrt{\frac{L_a}{C_a}};\quad A_b=\sqrt{\frac{L_b}{C_b}};\quad \ldots A_m=\sqrt{\frac{L_m}{C_m}}\\
&\omega_a=\frac{1}{\sqrt{L_aC_a}};\quad \omega_b=\frac{1}{\sqrt{L_bC_b}};\\
&\qquad\ldots \omega_m=\frac{1}{\sqrt{L_mC_m}}\\
&C_a=\frac{1}{\omega_aA_a};\quad C_b=\frac{1}{\omega_bA_b};\quad \ldots C_m=\frac{1}{\omega_mA_m}\\
&L_a=\frac{A_a}{\omega_a};\quad L_b=\frac{A_b}{\omega_b};\quad \ldots L_m=\frac{A_m}{\omega_m}
\end{aligned}\right\} \quad (11)$$

By equating coefficients of equations 9*b* and 10, the circuit elements of equivalent networks can be stated directly in terms of the zeros and slopes:

$$\left.\begin{aligned}
&\sqrt{\frac{L_a}{C_a}}=\frac{2}{\omega_a\frac{\Delta B}{\Delta\omega_a}};\quad \sqrt{\frac{L_b}{C_b}}=\frac{2}{\omega_b\frac{\Delta B}{\Delta\omega_b}};\\
&\qquad\ldots\sqrt{\frac{L_m}{C_m}}=\frac{2}{\omega_m\frac{\Delta B}{\Delta\omega_m}}\\
&\omega_a=\frac{1}{\sqrt{L_aC_a}};\quad \omega_b=\frac{1}{\sqrt{L_bC_b}};\\
&\qquad\ldots \omega_m=\frac{1}{\sqrt{L_mC_m}}\\
&C_a=\frac{1}{2}\frac{\Delta B}{\Delta\omega_a};\quad C_b=\frac{1}{2}\frac{\Delta B}{\Delta\omega_b};\ \ldots C_m=\frac{1}{2}\frac{\Delta B}{\Delta\omega_m}\\
&L_a=\frac{1}{\omega_a^2C_a};\quad L_b=\frac{1}{\omega_b^2C_b};\quad \ldots L_m=\frac{1}{\omega_m^2C_m}
\end{aligned}\right\} \quad (12)$$

In the preceding discussion the basic equivalent networks have been derived from network-response equations based on the Heaviside expansion theorem. By postulating that every network can be reduced to one of the basic forms of equivalent circuits, it is possible to derive the network response equations 3 and 4 without the use of the expansion theorem. Thus, the equivalent circuits provide not only a means of visualizing the network response but also the basis for an alternative derivation of the response equations.

Alternating Voltages and Currents Applied

The response of a network to alternating voltages or currents can be obtained from the response to unit voltage or current by superposition, or from the basic equivalent networks. The equations relating the alternating and unit-function responses have been derived and are included here for reference.

(a). Alternating Voltage Applied

Let

$$i(t)=\frac{t}{L_o}+\left[\frac{C_o}{t}\right]_{t=0}+\sum_{k=1}^{k=n} A_k \sin \omega_k t$$

be the network response to unit voltage. The response of the network to the sudden application of the voltage $E(t)=E \cos(\omega_o t+\phi)$ is then defined by the following equation:

$$\left.\begin{aligned}
i(t)=&\left[\frac{E}{\omega_oL_o}\sin(\omega_o t+\phi)-\frac{E}{\omega_oL_o}\sin\phi\right]+\\
&\left[-E\omega_oC_o \sin(\omega_o t+\phi)+\left(\frac{EC_o\cos\phi}{t}\right)_{t=0}\right]+\\
&\left[-E\sum_{k=1}^{k=n}\frac{\omega_o\omega_kA_k}{(\omega_k^2-\omega_o^2)}\sin(\omega_o t+\phi)+\right.\\
&\left.E\sum_{k=1}^{k=n}\frac{\omega_k^2A_k}{(\omega_k^2-\omega_o^2)}\left(\cos\phi\sin\omega_k t+\frac{\omega_o}{\omega_k}\sin\phi\cos\omega_k t\right)\right]
\end{aligned}\right\} \quad (13)$$

Each of the bracketed terms in the preceding equation is the a-c solution corresponding to one term in the unit voltage response equation or for one branch of the equivalent network. The first term in each bracket is a component of the steady-state response of the network, whereas the second term is in each case a transient response. In many cases less work is involved in determining the steady-state response directly from the network reactance at $\omega=\omega_o$. If this reactance is equal to $X(j\omega_o)$, then the steady-state response is equal to

$$\frac{E}{X(j\omega_o)} \cos(\omega_o t+\phi)$$

Table VI. Alternating Current Applied

(1) ω	(2) ωL_2	(3) $1/\omega C_2$	(4) (2)+(3)	(5) 1/(4)	(6) ωC_1	(7) $1/\omega L_1$	B (5)+(6)+(7)
				$x10^{-4}$	$x10^{-4}$	$x10^{-4}$	$x10^{-4}$
2,701	2,701	−3,702.30	−1,001.30	9.987	27.01	−37.023	−0.026
2,702	2,702	−3,700.96	−998.96	10.011	27.02	−37.009	+0.021
3,701	3,701	−2,701.97	999.03	−10.009	37.01	−27.019	−0.018
3,702	3,702	−2,701.24	1,000.80	−9.992	37.02	−27.012	+0.016
377	377	−26,500	−26,100	0.383	3.77	−265	−261

$\omega_a=2{,}701$ $\qquad \omega_b=3{,}701$

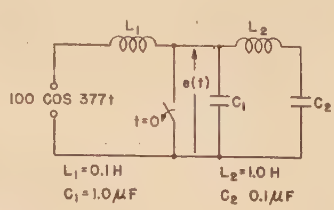
$\frac{\Delta B}{\Delta\omega_a}=0.047\times10^{-4}$ $\qquad \frac{\Delta B}{\Delta\omega_b}=0.034\times10^{-4}$

$A_a=157$ $\qquad A_b=159$

Fault current $=\frac{100}{j37.7}\cos 377t$

$=2.65 \cos\left(377t-\frac{\pi}{2}\right)$

$I(t)=2.65 \cos\left(377t-\frac{\pi}{2}\right)$



Steady-state component of $e(t)=\frac{2.65}{-j0.0261}\cos\left(377t-\frac{\pi}{2}\right)=102 \cos 377t$

$e(t)=102 \cos 377t-59 \cos 2{,}701t-44 \cos 3{,}701t$

(b). Alternating Current Applied

Let

$$e(t)=\frac{t}{C_s}+\left[\frac{L_s}{t}\right]_{t=0}+\sum_{k=1}^{k=n} A_k \sin \omega_k t$$

be the response for unit current applied. The network response to the current $I(t)=I\cos(\omega_o t+\phi)$ is then defined by the following relations:

$$\begin{aligned} e(t)=&\left[\frac{I}{\omega_o C_s}\sin(\omega_o t+\phi)-\frac{I}{\omega_o C_s}\sin\phi\right]+\\ &\left[-I\omega_o L_s\sin(\omega_o t+\phi)+\left(\frac{IL_s\cos\phi}{t}\right)_{t=0}\right]+\\ &\left[-I\sum_{k=1}^{k=n}\frac{\omega_o\omega_k A_k}{(\omega_k^2-\omega_o^2)}\sin(\omega_o t+\phi)+\right.\\ &I\sum_{k=1}^{k=n}\frac{\omega_k^2 A_k}{(\omega_k^2-\omega_o^2)}\left(\cos\phi\sin\omega_k t+\right.\\ &\left.\left.\frac{\omega_o}{\omega_k}\sin\phi\cos\omega_k t\right)\right] \end{aligned} \quad (14)$$

Each of the bracketed terms in the preceding equation is the a-c solution corresponding to one term of the response equation for unit current applied, or for one branch of the equivalent network. The first term in each bracket is the steady-state response, and the second term is the transient response. The steady-state response can also be obtained from the network admittance for $\omega=\omega_o$. If this admittance is represented by $B(j\omega_o)$, the steady-state response is equal to

$$\frac{I}{B(j\omega_o)}\cos(\omega_o t+\phi)$$

One special case of the foregoing solution is of general interest, the case where the applied current is equal to $I\sin\omega_o t=I\cos(\omega_o t-\pi/2)$:

$$\begin{aligned} e(t)=&\left[-\frac{E}{\omega_o C_s}\cos\omega_o t+\frac{I}{\omega_o C_s}\right]+\\ &\left[I\omega_o L_s\cos\omega_o t\right]+\\ &\left[I\sum_{k=1}^{k=n}\frac{\omega_o\omega_k A_k}{(\omega_k^2-\omega_o^2)}\cos\omega_o t-\right.\\ &\left.I\sum_{k=1}^{k=n}\frac{\omega_o\omega_k A_k}{(\omega_k^2-\omega_o^2)}\cos\omega_k t\right] \end{aligned} \quad (15)$$

Illustrative Examples

The method of analysis just described is illustrated in Table IV for a relatively simple circuit. The problem in this case is to find an analytical expression for the current in the inductance L_1 when unit voltage is suddenly applied to the driving-point terminals. The zeros are first located approximately by rough slide-rule calculations, preferably using a plot of the reactance curve as a guide. The reactance of the circuit, as viewed from the driving point, is calculated by combining the reactance of the individual branches as illustrated in the table. It should be noted that it is not necessary to obtain the complete reactance curve but only those parts of the curve in the vicinity of zero reactance. Only two points near each zero are included in the illustration, these points having been arrived at after other preliminary points were obtained. The reactance curves need not be plotted except as a guide; all necessary information is included in the tabulated data. In this example $\Delta\omega$ was taken as one radian per second in order to obtain an accurate evaluation of $\Delta X/\Delta\omega$. The angular velocity increment to use in any particular case depends upon the network characteristics and upon the accuracy desired. It should be stressed that the reactance calculations should be carried out to a high degree of accuracy if a high degree of accuracy is desired in the final network response. However, if practical engineering accuracy is sufficient then less accuracy is required in calculating the reactances.

After the zeros and slopes are obtained the response is calculated by substituting these values in equation 3. As the network considered in this example is of type I, $i_o(t)$ is equal to zero and so the complete response is as given at the bottom of Table IV.

The accuracy of the method can readily be checked by noting the initial conditions. At $t=0$, the voltage across C_1 is zero, so the applied unit voltage must be consumed across L_1, that is, $L_1\frac{di(t)}{dt}$ should be equal to unity at $t=0$. For Table IV, the check gives

$$L_1\frac{di(t)}{dt}=0.1(2{,}701\times0.00212+3{,}701\times0.00115)=0.998 \text{ at } t=0$$

which is accurate to within 0.2 of one per cent.

In the general case the calculated responses for networks of types I and II are checked by noting that $L_s\frac{di(t)}{dt}=1.0$ at $t=0$, assuming unit voltage applied, where L_s is the equivalent inductance between the driving-point terminals with all capacitances short-circuited.

In special cases some difficulty may be experienced in locating the zeros. For example, the reactance of the network in Table V was calculated at $\omega=33{,}100$ and 33,200 and the resulting reactances were both positive and decreasing in magnitude. These results incorrectly may be assumed to indicate that a zero would be present at some higher angular velocity, whereas the zero is actually between these values of angular velocity. When such cases are encountered much time is usually required to locate the zero. However, when the zeros are hard to locate, the slope of the reactance curve is large and as a result the transient current corresponding to the zero may be an unimportant component of the network response. In Table V the second component of current is only 0.00723 per cent of the first component and so is insignificant. This situation could have been predicted by checking the first component of current with the initial conditions. Neglecting the second term,

$$L_1\frac{di(t)}{dt}=0.1(0.00332)(3{,}014)=1.0 \text{ at } t=0$$

which satisfies the boundary conditions. It is very helpful to make these checks for each component as soon as it is obtained, in order to obtain not only a check on the integrity of the calculation but also a continuous indication of the relative importance of the remaining terms of the solution.

The problem of calculating the transient voltage following the sudden opening of a switch can be treated in terms of a suddenly applied current. Considering the network in Table VI, the current flowing in the closed switch is equal to $+2.65\sin 377t$. The current in the switch can be reduced to zero, which is equivalent to opening the switch, by inserting a current $+2.65\sin 377t$ in a direction opposite to the initial current. In making the calculations with the method of zeros and slopes, the response to unit current is first obtained, as illustrated in Table VI, by determining the zeros and slopes of the susceptance curves. The response to the current $+2.65\sin 377t$ is then found by using the relations given in part *b* of the preceding section. The steady-state component of the recovery voltage was found by first calculating the network susceptance at $\omega=377$.

Extensions of the Method

In the paper the calculating procedure has been illustrated only for the zeros and slopes derived from the constants of the networks. However, it is possible and sometimes convenient to form the analytical expression for the impedance (or admittance) function and its derivative and to use the first or both of these to determine the zeros and slopes. The method of analysis and calculating procedure have been discussed only for prob-

lems involving driving-point functions. However, they are also applicable to problems involving transfer functions, for example, the determination of current in a particular branch of a network resulting from the application of potential to the driving-point terminals. The procedure is the same except that for the latter case the zeros and slopes are obtained from the transfer function instead of the driving-point function. Angular velocity-reactance (or susceptance) curves, derived from measurements on a miniature-system setup or even from an actual system instead of from calculated data as described in the paper, can be used for the determination of zeros and slopes.

Appendix

The response of a network to a suddenly applied unit voltage is given by the Heaviside expansion theorem as follows:

$$i(t)=i_o(t)+\sum_{k=1}^{k=2n}\frac{e^{p_kt}}{p_kZ'(p_k)} \tag{1}$$

In this form of the expansion theorem the function $i_o(t)$ defines the steady-state network solution and that part of the transient solution determined by the external zeros. Referring to the second term, $Z(p)$ is the impedance function of the network as viewed from the driving point and $Z'(p)$ is the first derivative of the impedance function with respect to p. The summation indicates that there are as many terms in the transient solution (neglecting the $i_o(t)$ term) as there are roots of the equation $Z(p)=0$. The quantity p_k is a general expression for the roots of $Z(p)=0$.

In a no-loss network the roots of $Z(p)=0$ are pure imaginaries appearing in conjugate pairs and so can be represented by $p=\pm j\omega$. It should also be noted that in no-loss network $Z'(j\omega_k)=Z'(-j\omega_k)$. Substituting these relations into the expansion theorem reduces equation 1 to

$$i(t)=i_o(t)+\sum_{k=1}^{k=n}\frac{e^{j\omega_kt}}{j\omega_kZ'(j\omega_k)}-\sum_{k=1}^{k=n}\frac{e^{-j\omega_kt}}{j\omega_kZ'(j\omega_k)} \tag{2}$$

This form of the theorem can then be converted to the trigonometric form, remembering that

$$\frac{e^{j\omega t}-e^{-j\omega t}}{2j}=\sin\omega t,$$

$$i(t)=i_o(t)+\sum_{k=1}^{k=n}\frac{2}{\omega_kZ'(j\omega_k)}\sin\omega_kt \tag{3}$$

In a no-loss network $Z(j\omega)$ is recognized as the alternating-current reactance of the network as measured at the driving point, and $\omega_k=\omega_1, \omega_2 \ldots n$ are the angular velocities that give zero reactance. The term $Z'(j\omega)$ is, therefore, the slope of the reactance curve plotted as a function of $j\omega$, and $Z'(j\omega_k)$ is the slope at $\omega=\omega_k$. It becomes apparent, therefore, that the constants of integration, or the coefficients of the series of sine functions, depend only on the angular velocities resulting in zero reactance, and on the slope of the reactance curve at these corresponding velocities.

In making numerical computations, equation 3 is more useful in its approximate form,

$$i(t)=i_o(t)+\sum_{k=1}^{k=n}\frac{2}{\omega_k\dfrac{\Delta X}{\Delta\omega_k}}\sin\omega_kt \tag{4}$$

or with alternative notation,

$$i(t)=i_o(t)+A_1\sin\omega_1t+A_2\sin\omega_2t+\ldots A_n\sin\omega_nt \tag{5}$$

where

$$A_1=\frac{2}{\omega_1\Delta X/\Delta\omega_1};\quad A_2=\frac{2}{\omega_2\Delta X/\Delta\omega_2};\ldots$$

$$A_n=\frac{2}{\omega_n\Delta X/\Delta\omega_n}$$

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The Cause and Control of Some Types of Switching Surges

T. W. SCHROEDER
ASSOCIATE AIEE

Among the several causes other than lightning which can result in power-system overvoltages, switching surges rank high in importance. In the past the magnitudes of switching surges have been considered on a statistical basis, but the need for frequent switching operations naturally led to their study and to analyses of their causes and magnitudes.

Switching surges are transient overvoltages caused by closing or opening a circuit and can vary in magnitude and duration over wide ranges. As contrasted with lightning surges which are generally impulsive and unidirectional in character, switching surges are oscillatory in nature, of frequency anywhere from very high to fundamental or subharmonic frequency, and of duration from a small portion of a cycle to a relatively long time which may be governed by the time between the opening or closing of successive phases on a three-phase system. Where steepness of voltage wave front is a consideration, it should be recognized that surges caused by switching on certain circuits may have wave fronts steeper than the ordinary lightning surge.

Switching in circuits supplying ungrounded transformer banks can result in high voltages during the switching period if the interval between closing or opening the first and last phase in a three-phase system is long enough for the intermediate steady-state voltage conditions to be attained. This condition may obtain during single-phase switching on a three-phase system, or when one or two conductors are open by virtue of conductor breaks or blown fuses. The voltages obtained under these conditions have been studied and reported[1] for a wide range of constants. The principal constants which apply are the transformer magnetizing reactance, the system capacitance between the switch and the transformer, and the degree of transformer saturation. This paper in general, will, consider over-voltages other than these, which occur even with reasonably simultaneous action of the switches in the three poles. Primary consideration will be given to transient voltages caused by normal rather than fault switching operations.

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T. W. Schroeder is in the central-station engineering department of General Electric Company, Schenectady, N. Y.

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Conclusions Drawn

Some of the conclusions which may be drawn from the analyses and discussion are as follows:

1. When a switch on an unfaulted power system is being closed, transient voltages as

high as nearly twice normal line-to-neutral voltage may be obtained. This assumes that closing of the three poles of the switch is reasonably simultaneous, as would be true of the ordinary circuit breaker.

2. Transformer windings can be very effective in discharging the stored energy of a capacitive circuit being opened and thus reduce the possibility and magnitude of restriking overvoltages.

If the breaker is connected to the line, cable, or capacitor bank through a delta-wye-grounded transformer bank as shown in Figure 3B, the overvoltage under restriking conditions will be limited to less than twice normal line-to-ground voltage if the transformer has a kilovolt-ampere rating equal to or greater than the charging kilovolt-amperes of the capacitive circuit. Simultaneous clearing of the three phases is not required for this statement to hold true.

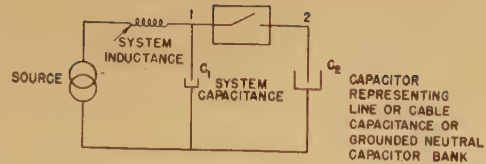
Where switching is done at line voltage, transformer windings switched with the line, as shown in Figure 3A, are also effective if the ratio of the capacitive reactance being switched to the transformer magnetizing reactance, both on a line-to-neutral basis (this may be considered the ratio of transformer magnetizing kilovolt-amperes to capacitance kilovolt-amperes), falls between the approximate limits of 0.0012 and 0.02 or 0.45 and ∞. In the region between 0.02 and 0.45, reasonably simultaneous opening of the three phases is essential to keeping the switching surge voltages low.

3. The prevention of overvoltages of harmful magnitudes, which may occur in certain limited fields on account of the forcing of the current zero by the circuit breaker on interrupting, may be accomplished by the use of shunt capacitors. The minimum capacitor kilovolt-ampere value for satisfactory performance depends upon the characteristics of the circuit, and the breaker, and the allowable overvoltage magnitude.

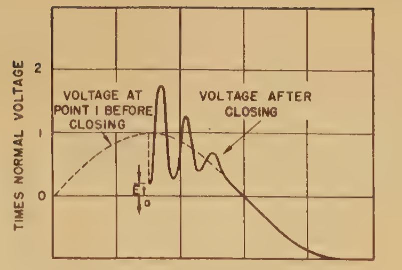
It is not intended to convey the impression that in every case of switching capacitive circuits or of switching magnetizing currents, special means need be used to prevent serious switching surge overvoltage. Although restriking may occur while opening capacitive circuits, such restriking will not necessarily produce overvoltages serious from the standpoint of circuit and apparatus insulation, particularly where adequate lightning-arrester protection is employed. This also is true of overvoltages produced by off-zero magnetizing-current interruption.

Switch-Closing Overvoltages

When an unenergized circuit is suddenly connected to an energized one, a voltage oscillation of twice (neglecting damping caused by losses) the voltage difference between them at the instant before closing occurs immediately following the closing operation. The reason for this is the presence of system capacitance, and it may be illustrated by Figure 1A which shows a one-line diagram of a system, a switch, and a capacitor. Assuming that the switch is closed at the peak of the generated voltage wave, a,



A. Equivalent circuit of system, switch, and a capacitance



B. Voltage transient on closing switch of Figure 1A at normal voltage crest

Figure 1. Illustrating switch-closing transient

in figure 1B, the voltage at point 1 instantaneously drops to a value given by

$$E_t = \frac{EC_1}{C_1 + C_2}$$

and an oscillation of voltage of amplitude $E - E_t$ then takes place about the fundamental voltages as shown, rapidly decaying, in general, because of losses and other system damping such as loads. The oscillation frequency is determined by the system inductance and total capacitance.

In the limit, if C_1 is reduced to zero, E_t becomes zero, the oscillation amplitude becomes E, and the total voltage on the capacitor reaches $2E$, if the switch is closed at the voltage peak and damping is neglected.

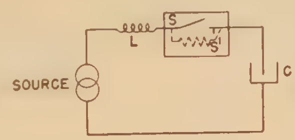
It has been shown[2] that, if the capacitance being switched, C_2, in Figure 1A, is replaced by a line of distributed inductance and capacitance, a similar voltage oscillation will appear all along the line. In the case of the line, however, the oscillation will appear at various points along the line at successive intervals of time and will require a time to reach the far end, depending primarily on the line length and its inductance and capacitance per unit of length. The frequency of the oscillation is also governed by these quantities.

Switch-Opening Overvoltages—Interrupter Restriking

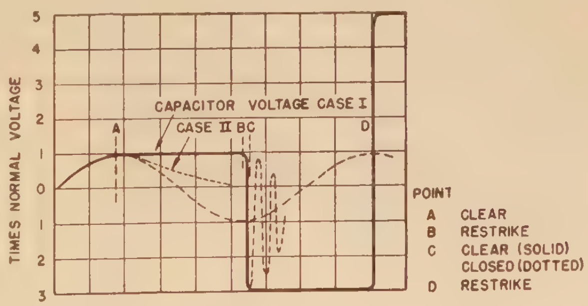
Switch-opening transients are generally negligible if the interrupter opens the circuit at or very near current zero and does not restrike. Restriking takes place in any interrupter if the voltage across its contacts following an interruption recovers more rapidly than the dielectric strength of the insulating medium. When a fault on a solidly grounded system having small capacitance is being cleared by an interrupter, it may restrike several times following an attempted clearing at a current zero, until the contacts have gotten sufficiently far apart that they can withstand the recovery voltage transient of less than twice normal crest line-to-ground voltage, which will occur at each attempted clearing. This restriking is normal and expected and is not harmful either to the breaker or the system. (On a reactance-grounded system, restriking when clearing unbalanced faults can result in severe overvoltages if the ratios of zero-to-positive-sequence inductive reactance are high.[3])

However, under certain conditions, overvoltages of a serious nature may occur as a result of delayed restriking when opening the normally low balanced charging current of capacitance circuits, notably transmission lines and large banks of capacitors. The results of recent studies on capacitor[4] and transmission-line[5] switching, in which this phenomenon has been analyzed, have been reported. For the sake of completeness, a brief discussion of the mechanism by which these overvoltages are obtained follows.

Referring to Figure 2A which represents



A. Equivalent circuit of system, switch, and a capacitance

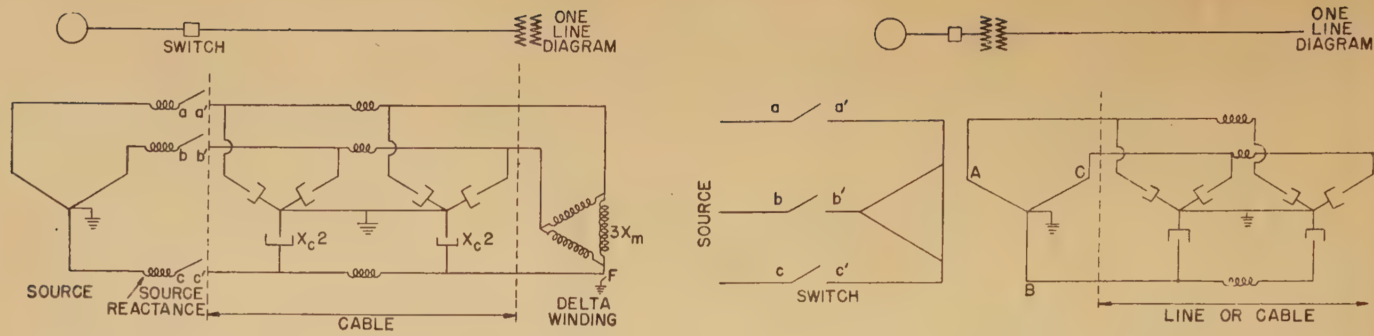


B. Voltage transient resulting from a restrike of switch in Figure 2A 180 electrical degrees after interruption at normal current zero (voltage crest)

Case I—Without resistor across switch

Case II—With resistor across switch, final interruption by opening switch S' of Figure 2A

Figure 2. Illustrating transient voltage resulting from restriking when opening a capacitive circuit



A. Three-phase cable circuit with saturable delta winding switched together

B. Three-phase cable circuit with delta–wye-grounded transformer between switch and cable

Figure 3. Miniature-system (transient-analyzer) connection diagrams

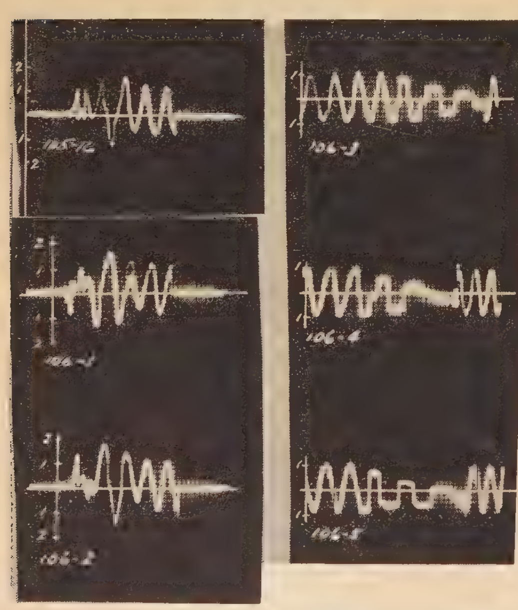
one phase of a system of inductance L supplying a capacitance load, C, let switch S be opened. At a current zero (voltage crest point A of Figure 2B), the current will be interrupted, leaving a full charge on the capacitor. The voltage across the interrupter contacts will grow from zero to twice normal in 180 electrical degrees, as shown by the difference of the stored capacitor charge and the system normal frequency voltage. If restriking occurs during this period, a voltage oscillation will result of magnitude equal to the switch voltage, about the fundamental, resulting in a total voltage of nearly three times normal (neglecting damping) if the restrike occurs when the switch volts are a maximum, as shown at point B. The oscillation will be at natural frequency, and at its first peak (point C), the arc current will be zero. If conditions within the interrupter are conducive to the arc's remaining extinguished at this point, the switch voltage will grow from twice to four times normal in the next 180 degrees, and a restrike at point D would result in a voltage of nearly five times normal. If the arc does not go out at C, the natural frequency voltage will gradually decay as shown by the dashed curve.

It should be remembered that whether the interrupter will restrike under this switching condition depends upon the magnitude of the capacitance current being interrupted and the interrupter characteristics. Tests[4] have established the amount of capacitance, in terms of kilovolt-amperes, that certain breakers in the voltage class 15 kv and below may interrupt with reasonable assurance against delayed restriking. Perhaps future tests will be desirable at higher voltages.

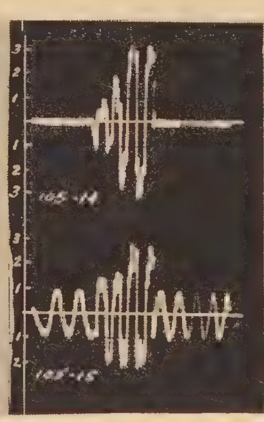
A study of Figure 2B indicates that whatever may be done to reduce the charge stored on the capacitor or line after the first clearing will help very materially, not only to prevent restriking when switching capacitive circuits, but also to reduce greatly the magnitude of the overvoltage obtaining if restriking does occur. The following paragraphs in this section will be devoted to discussing the various means which may be employed to bring about a reduction in the capacitor or line voltage within the half-cycle following the first interruption of the circuit.

1. Resistor Switching

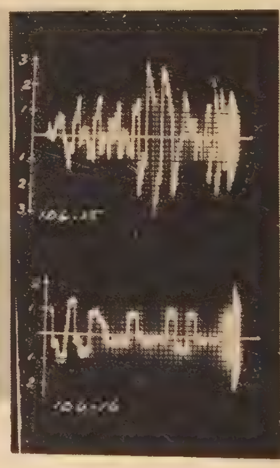
If, when the switch S of Figure 2A is opened, a relatively high resistance is



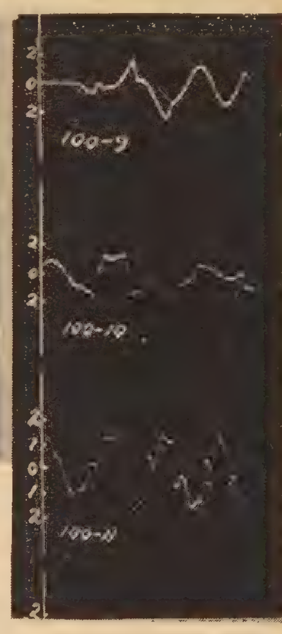
A. Opening all poles of Figure 3A, ideal clearing:
105-16—Voltage a-a'
106-11—Voltage b-b'
106-2—Voltage c-c'
106-3—Voltage a' to ground
106-4—Voltage b' to ground
106-5—Voltage c' to ground



B. Opening pole a-a' of Figure 3A only:
105-14 — Voltage a-a'
105-15 — Voltage a' to ground



C. Same as B, phase c faulted:
106-15—Voltage a-a'
106-16—Voltage a' to ground



D. Opening pole a-a' of Figure 3B:
100-9 —Volts a-a'
100-10—Volts a' to ground
100-11—Volts C to ground

Figure 4. Transient-analyzer oscillograms

All calibrations in times normal line-to-neutral voltage

inserted in the circuit by this action, as would be the case if the interrupter were shunted by a resistor as shown by the dotted lines, the capacitance is afforded a discharge path through sufficient resistance to permit considerable decay of its voltage as shown in Figure 2B, case II (dotted curve). This scheme not only reduces the possible overvoltages but also affords a relatively easy subsequent interruption of the circuit by switch S', because, since the resistor ohms are relatively high, the current will be nearly in phase with the voltage, the current will be small, and at current zero the circuit voltage will be near zero.

Reference 5 describes tests and analyses of an oil circuit breaker provided with such a resistor for reducing the overvoltage hazard when interrupting a long high-voltage line, and reference 2 similarly indicates performance of a breaker provided with resistors to assist in switching a large 13.8-kv capacitor bank. Reference 5 shows that it is possible to obtain a practical value of contact shunting resistance which will provide a very satisfactory means of solving the capacitance switching problem. It should not be inferred, however, that all existing breakers for switching long high-voltage lines need be provided with resistors.

2. Discharge Through Transformer Windings

In the case where, after the switch is opened, the capacitor or line is left with a transformer bank in shunt with it, the windings provide a discharge path for the energy stored in the capacitance. The effectiveness of the windings depends upon their size and whether they are delta—or grounded-wye—connected to the line.

(*a*). *Delta or Ungrounded-Wye Windings.* Refer to Figure 3A. This shows the connection diagram of a miniature-system representation of a cable having a total equivalent shunt capacitive reactance per phase of X_c ohms with the delta winding of an unloaded transformer connected to it. The transformer has a magnetizing reactance per phase of X_m on a line-to-neutral basis. As the first pole of the interrupter a-a' opens, the cable capacitance begins to discharge through the transformer, thus reducing the voltage across the switch initially, over the case of the cable or line alone. If the remaining poles of the interrupter subsequently clear "ideally"—each at its nearest available current zero, the switch voltage (across a-a') will never reach twice normal, and the tendency of the interrupter to restrike should be markedly reduced.

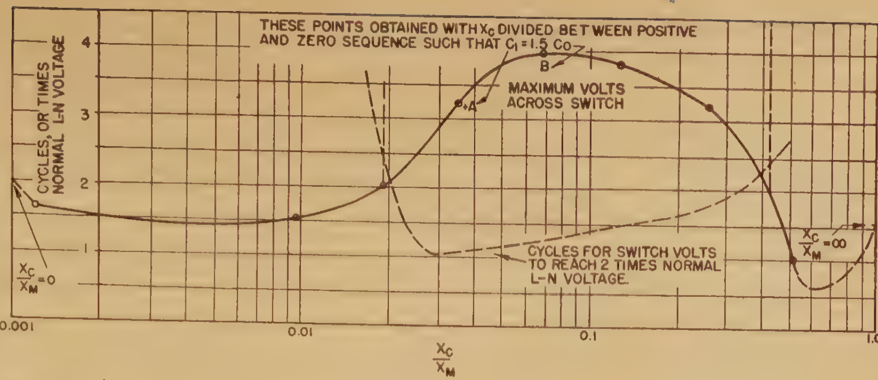


Figure 5. Summary of maximum switch voltages and time for switch voltage to reach twice normal line-to-neutral voltage when opening switch a-a′ of Figure 3A for various values of X_c/X_m

Oscillograms 105-16 through 106-5 of Figure 4 show switch and cable voltages for the case of ideal clearing for a ratio of $X_c/X_m = 0.052$. If the interval of time between successive pole clearings is too large, however, the switch and cable voltages may, depending upon the ratio X_c/X_m, become much larger than twice normal, because of series resonance between the capacitance of the unopened conductors and the saturable transformer reactance. These are the overvoltages mentioned previously and reported in reference 1. The time required for the switch voltage to reach twice normal again depends on the ratio X_c/X_m and is summarized for the first pole to clear in curve form, Figure 5, together with the steady-state values that the switch voltage will attain for various values of the ratios. Oscillogram 105-14 shows the voltage across the first pole (a-a') opening with the others remaining closed for a ratio of $X_c/X_m=0.052$. Oscillogram 105-15 gives the voltage of point a' to ground for this condition of interruption. It will be noted that it required about one cycle for voltage a-a' to reach twice normal line-to-neutral value, and, if reasonably simultaneous clearing of the other poles had occurred, pole a would have had a comparatively low voltage across it while opening. On the basis that similar results were obtained on the other poles, it was considered adequate to use the transient resulting from the first pole to open as a general criterion of the effectiveness of a delta winding to prevent the switch voltage from reaching at least twice normal in a short time. Also, only a general indication of results was necessary, since in general long times between successive pole openings cannot be tolerated.

It was interesting to note that, if a ground fault on one of the cable or capacitor phases as shown by F in figure 3A was being cleared, the transient voltages resulting from clearing the first of the unfaulted poles are somewhat reduced from the case of no fault. The reason for this is that a discharge path for the capacitance in addition to the delta winding exists by virtue of the fault. Oscillogram 106-15 shows the switch voltage (a-a') for $X_c/X_m=0.052$ for the condition of phase a being cleared, with a fault on phase c, and 106-16 shows the voltage of a' to ground for this condition.

The data in Figure 5 is for the condition of equal positive- and zero-sequence capacitances. To show the effect of a ratio of positive-to-zero-sequence capacitance of 3/2 (approximately the value for an overhead line), tests were made on the miniature system which indicated the effect to be one of moving the curves of Figure 5 slightly to the right.

(*b*). *Grounded-Wye Windings.* If the line or grounded-neutral capacitor is left with a grounded winding instead of an ungrounded one, it will be somewhat more effective than an ungrounded one, because it is capable of discharging both zero- and positive-sequence capacitance. A special case where a delta–wye-grounded transformer is interposed between the interrupter and line with the line or capacitor connected to the wye-grounded winding was investigated. Figure 3B shows the miniature-system connections. The case of a transformer large enough to carry the line-charging or capacitor kilovolt-amperes being interrupted alone resulted in voltages of less than twice normal from line to ground even with a restrike applied at a time when the switch voltage following first clearing was a maximum. Oscillograms 100-9 to -11 show miniature-system voltages for this case. This indicates the benefits to be gained by "low-side switching" of lines, cables, or capacitors.

Switch-Opening Overvoltages—Interruption Off Current Zero

Another means by which an overvoltage may be obtained on opening a circuit

is the studden stoppage of current flow before a normal-frequency current zero has arrived. Under this condition, the voltage obtainable is controlled by the circuit inductance and the rate of change of current caused by the interrupter thus forcing current zero. Overvoltages of this nature have been encountered in the process of interrupting transformer magnetizing currents which are small in magnitude, compared to the normal load current of both the transformer and the breaker. In particular, this problem has been encountered in connection with arc-furnace transformers which, because they are switched at no load frequently, are more apt to be subjected to this type of switching surge than other transformers, other things being equal.

Here again the tendency for the overvoltage to occur is dependent upon the characteristics of the interrupter at the magnitude of current to be switched. For a particular value of current a given interrupter might break the circuit at current peak (worst case), at or near zero, or anywhere in between.

Lightning arresters can be and have been used to protect transformers from this type of overvoltage, but it should be recognized that, if the interrupter cuts off at too high a current, the energy (LI^2) stored in the transformer may be greater than the arrester can dissipate without damage to itself.

An obvious means of preventing the possibility of this type of switching-surge overvoltage is to supply the magnetizing kilovolt-amperes of the transformer by means of capacitors, and switching the two together. The only no-load current to be switched if complete compensation is provided is loss current, which will be very small. It is not particularly important in this method whether either the transformer winding or the capacitor neutral is grounded.

Analysis (see appendix) and miniature-system tests indicate that it is not necessary to supply entirely the transformer magnetizing kilovolt-amperes in order to limit the transient voltage to acceptable values. In the appendix it is shown that for the circuit of Figure 6, representing a transformer winding protected by a capacitor, assuming interruption at current crest (worst case) gives a voltage oscillation across the transformer–capacitor combination whose equation is

$$E_p = -E\sqrt{\frac{X_c}{X_m}}\sin\left\{\sqrt{\frac{X_c}{X_m}}t\right\}$$

where

E = normal line-to-neutral voltage
X_c = capacitor line-to-neutral ohms per phase at fundamental frequency
X_m = transformer magnetizing line-to-neutral ohms per phase at fundamental frequency and normal voltage

The expression $\sqrt{X_c/X_m}$ may be written $\sqrt{\frac{\text{transformer magnetizing kilovolt-amperes}}{\text{capacitor kilovolt-amperes}}}$ and gives the crest value of the voltage oscillation following interruption and the order of its natural frequency. Thus a capacitor kilovolt-ampere of 25 per cent of the transformer magnetizing kilovolt-amperes will limit the overvoltage under the worst condition of interruption, at current crest, to twice normal, and the oscillation will be at second harmonic frequency.

Although this analysis is made on the basis of an equivalent single-phase circuit with saturation of the transformer neglected, miniature-system tests on a three-phase setup including saturation indicates that the results obtained apply practically whether the transformer and/or the protective capacitors are grounded or ungrounded.

Appendix—Magnetizing-Current Interruption Off Zero

In the equivalent circuit of Figure 6, in which a shunt capacitor is connected to the terminals of the transformer, let

Per unit transformer magnetizing kilovolt-amperes $= \text{kva}_m = \frac{1}{X_m}$

Per unit capacitor kilovolt-amperes $= \text{kva}_c = \frac{1}{X_c}$

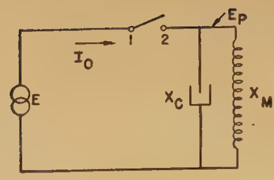


Figure 6. Equivalent circuit of a source, a switch, and an unloaded transformer shunted by a protective capacitor

The current flowing before the switch is opened is

$$I_0 = \frac{E(X_c - X_m)}{X_c X_m}$$

Assuming interruption at current crest (worst case), this may be represented mathematically by inserting the negative of this current through the switch, letting $t=0$ at current crest and the switch voltage will be $I_c \cos \omega t Z(p) = E_{12}$. The voltage across the transformer–capacitor combination is

$$E_p = E + E_{12}$$
$$= E \sin \omega t + I_0 \cos \omega t Z(p)$$

in operational form this is, where

$$\omega_0^2 = \frac{1}{CL}$$

$$E_p = \frac{E\omega p}{p^2+\omega^2}1 + I_0\frac{\omega_0^2 p^2}{(p^2+\omega^2)} \times \frac{pL}{(p^2+\omega_0^2)}1$$

And the solution of this is

$$E_p = E\sin\omega t - \frac{I_0 L \omega_0^2 \omega}{(\omega_0^2-\omega^2)}\left[\sin\omega t - \frac{\omega_0}{\omega}\sin\omega_0 t\right]$$

which reduces to

$$E_p = -E\sqrt{\frac{X_c}{X_m}}\sin\left(\sqrt{\frac{X_c}{X_m}}t\right)$$

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Automatic Voltage Compensator for Resistance Welding Control

E. M. CALLENDER
ASSOCIATE AIEE

R. S. PHAIR
ASSOCIATE AIEE

AMONG the more recent developments in a-c resistance welding control is the voltage compensator. This compensator is an auxiliary control used in conjunction with a standard type of phase-controlled welding panel, the circuit of which is shown in Figure 1. The function of the voltage compensator is to maintain the weld current substantially constant irrespective of line-voltage variations. The inability to obtain new feeders and the burden of added loads in war plants have in many cases made the power systems incapable of supplying several welding machines simultaneously without experiencing an appreciable drop in line voltage. Welding heat, which varies with current squared, will be reduced by 19 per cent for a ten per cent drop in line voltage; a line drop of 20 per cent will reduce the welding heat 36 per cent which is intolerable for consistent welding. These conditions can be remedied by the addition of a small inexpensive auxiliary control, such as the voltage compensator. The regulation of many overworked high reactance welding feeders can then be tolerated because consistent welds will be produced, whereas without proper compensation, poor welds would inevitably result. A compensator unit is shown in Figure 2 installed just in front of the current transformer in the bottom of a portable welding-machine control panel.

Requirements of the Compensator

First, let us consider where the compensator is required. Small voltage variations are permissible, since welding pressure, electrode shape, metallic finishes and other conditions may vary to some degree. Information in previous papers indicates that a five per cent voltage variation will not seriously reduce the weld strength of many types of steel.[1,2] Heavier line drops will probably require the voltage compensator. Its chief utility is on lines having more than one welding machine, since the line drop can be taken into account when adjusting current for one machine. Where line voltage drifts up and down, the compensator becomes advantageous with just one machine.

The next requirement is that the control operate entirely automatically. In operation, any gradual drift of the power-supply voltage will be continuously corrected, so that upon initiation of a spot weld, the initial cycles automatically start at the corrected heat setting. It is also important that the transient rate of response to sudden voltage changes should be as rapid as possible. This rapid response is very necessary in adapting such a control to seam and short-timed spot welding, where a slow response would result in relatively large variations between spots. It is, therefore, equally important that this voltage compensator be capable of nullifying the effects of transient as well as gradual power-supply fluctuations.

Other desirable features include simplicity, ease of connecting and adjustment, and, finally, low cost.

Phase-Controlled Welding Circuits

In order to understand how this voltage compensator works, it will be advantageous to review a standard phase-controlled welding circuit. Common to all electronic a-c welding control circuits are two power tubes, usually of the ignitron type, which are connected in an inverse parallel or back-to-back arrangement as shown in Figure 3. The function of these tubes is to serve as an electronic switch in controlling the power delivered to the welding machine. In this two-tube arrangement a-c power may be passed, since one tube may be rendered conductive in one direction, while the other tube, connected in reverse manner, permits conduction in the other direction. To render an ignitron conductive, a current must be passed through its ignitor or control element. This firing current is furnished by a thyratron-type tube which is capable of delivering high currents for short intervals. The welding control is designed to control these firing tubes, which in turn render the power tubes conductive.

When phase control is added to this electronic switch, the point at which this conduction is initiated is varied so that the power tubes carry current for a given preset portion of each half cycle. The particular phase-control circuit to which the line-voltage compensator was applied is shown in an elementary manner in Figure 1. For simplicity, all grid to cathode capacitors and transient arresting resistors have been left out. The net grid bias voltage on the firing tubes (see Figures 3 and 4) is the sum of four separate voltages:

1. An a-c "hold-off" bias of 300 volts supplied by transformer $T1$.
2. An a-c "turn-on" bias of 220 volts supplied by grid transformer $T3$.
3. A peaked triggering voltage of approximately 200 volts peak from transformer $T2$.
4. A self-rectified d-c voltage across capacitors $C3$ and $C4$ which appears there as a result of grid current flowing during the time the grid was positive.

Normally transformer $T3$ is de-energized, rendering the firing tubes nonconducting by virtue of the negative grid bias throughout the period of positive plate voltage as shown in Figure 4*A*. Upon energizing grid transformer $T3$ (Figure 4*B*) the net a-c bias is reduced such that the peak of firing voltage from transformer $T2$ exceeds the critical grid voltage of the firing tubes, permitting them to be conductive and thereby initiating the power tubes. For accurate and synchronous timing, transformer $T3$ is energized from a thyratron timing circuit, but the time could be controlled by a simple pushbutton hand switch. The phase setting of the $T2$ firing peak is determined by a resistance-capacitor bridge in which the resistance element $R7$ is adjustable for changing the phase of the peaked firing voltage. The components in series with the primary winding of the peaking transformer $T2$ form a filter to insure only one positive firing peak each half cycle. This circuit is resonant so that voltage and current are approximately in phase. However, the peaked voltage of $T2S$ which is about 10 degrees wide occurs at current zero and is therefore 90 degrees out of phase with the phase-shifting voltage vector.

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E. M. CALLENDER and R. S. PHAIR are electrical engineers in the methods and equipment division, Edward G. Budd Manufacturing Company, Philadelphia, Pa.

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The compensator may be adapted to any standard panel by three connections, 6, 7B (reconnection from 7), and 8 as shown in Figure 1.

Operation of the Compensator

The fundamental purpose of this compensator is to advance automatically the phase of the "peaker" firing voltages when the line voltage drops, and vice versa, to retard the phase should the line voltage increase. This is done by replacing the fixed transformer side of the resistance-capacitor phase-shift bridge with a voltage-dividing network which will vary in accordance with changes in line voltage. Referring to the elementary diagram of the compensator, Figure 5,

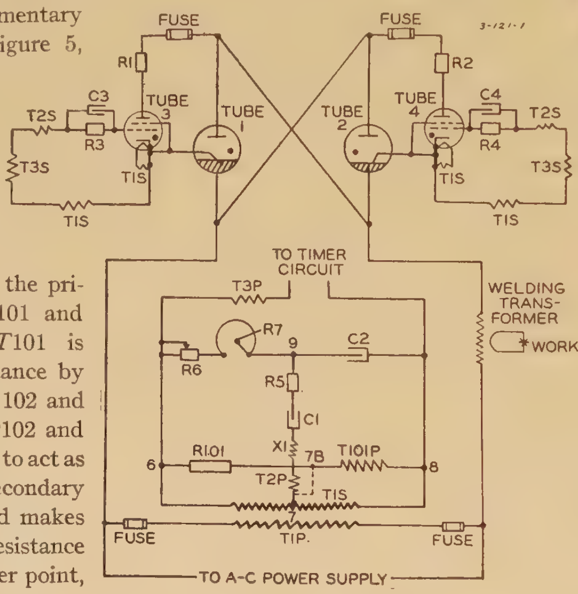


Figure 1. Simplified diagram of phase-controlled welding circuit

the voltage divider consists of the primary winding of transformer $T101$ and resistor $R101$. Transformer $T101$ is made to act as a variable resistance by the action of the shorting tubes 102 and 103. The grid voltage of tubes 102 and 103 when varied causes the tubes to act as a variable resistance load in the secondary winding of transformer $T101$ and makes $T101$ appear substantially as a resistance in the voltage divider. The center point, 7B, of the voltage divider may be shifted back and forth by some magnitude e_c as shown in Figure 6. This causes a shift in the vector relation between vector 6–8 which corresponds to the phase of the line voltage, and vector 7B–9 which in turn determines the phase of the peaked firing voltage $T2S$ of transformer $T2$ with respect to the line voltage. When the grids of tubes 102 and 103 are made less negative, tubes 102 and 103 appear as a lower resistance load and hence cause point 7B to move toward point 8, advancing the phase of the firing voltage vector $T2S$ with relation to vector 6–8. In a reverse manner the phase of the firing peak may be retarded by making the grids of tubes 102 and 103 more negative.

To obtain the operating grid voltage, transformer $T102$ is connected directly across the line supply and its output voltage rectified by tube 101. By making time constants of the filter on the output of the rectifier voltage relatively low, this d-c voltage can be made to follow closely all fluctuations in the line supply. D-c output of the rectifier provides a reference voltage across the voltage regulating bridge (tubes 104 and 105), which at normal line condition give zero voltage across points 20 and 21. A d-c signal voltage directly proportional to the line-voltage change appears across these bridge output terminals.

It is necessary next to investigate the amount of compensation or phase shift required to maintain the welding current constant for a given line-voltage drop at various power factors of the welding circuit and per cent current settings. The higher the power factor, the greater will be the phase shift required; and likewise, the larger the percentage of full-phase current flowing, the greater will be the shift required to maintain the current. Figure 7 illustrates the approximate phase shift requirements to correct for a 10 per cent line-voltage drop for various power factors and per cent current settings. It should be apparent that once full-phase firing has been reached, no additional compensation can be obtained. Hence, if the line voltage falls to 90 per cent of its normal value, a maximum (depending on power factor) of approximately 95 per cent current can normally be delivered to the welding machine. The compensated timer requires a five per cent, and in some cases ten per cent, zero current gap to allow for high heat compensation by phase advance. It is, however, usual practice to include this slight zero current gap at full heat in most welding machines so that any changes in secondary power factor will not permit heat-control settings to range above 100 per cent sine wave current. Unstable operation might result in operating practice unless this slight open current gap was adjusted for the ordinary welder control panel.

Figure 8 shows how the setting of $P101$

Figure 2. Panel with I^2T recorder and compensator installed

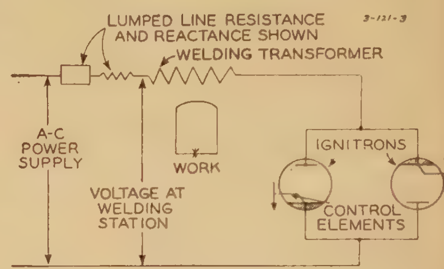


Figure 3. Typical welding power circuit

is used to obtain a greater rate of compensation at higher than normal power factors. $P101$ is adjusted so that there is an unbalance as much as 20 per cent between the voltages 6–7B and 7B–8. When a voltage drop occurs, point 7B is moved toward 8 by the amount e_c. This produces an angular advance in the trigger firing voltage of a_1, a_2, and a_3 for high, intermediate, and low heat settings. For the range of 20 per cent to 60 per cent power factor, which covers most resistance welding setups, a_1 is substantially greater than a_2 and a_3 without too much unbalance between voltages 6–7B and 7B–8. Thus $P101$ is adjusted to obtain a curving characteristic of phase shift versus heat setting. $P101$ is used as a bridge sensitivity control and therefore acts to determine the slope of the characteristic curve. By correlating the two adjustments it is possible to obtain a compensator characteristic which closely approaches the requirements shown by Figure 7. That this has been accomplished is borne out by experimental results which follow.

Data on Operating Performance

Referring to Figure 7, it is seen that the compensator must respond over a

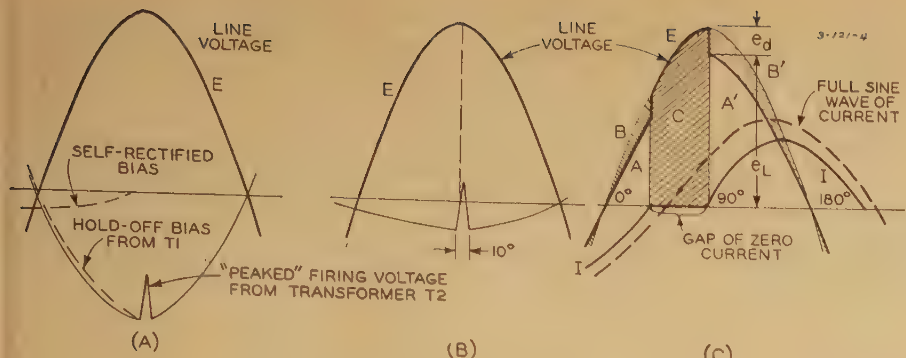


Figure 4A. One firing tube control voltage—standby

Figure 4B. Same firing tube voltage—for welding. Resultant grid firing voltage shown

Figure 4C. One half-cycle welding voltage and current showing instantaneous line drop

wide range of phase-angle advance for a variety of conditions that depend primarily on heat current setting and power factor. It must also be taken into account that the regulation may vary considerably, depending on the number of other machines that "hit" simultaneously. Differing portions may also be "chopped" off the useful area of the line-voltage wave as governed by the fractional heat dial settings of these machines. Then, too, interfering welders may also "come and go" early or late during a weld period. Fortunately, the control bridge circuit is very sensitive and responds correspondingly to all degrees of regulation, handling instantaneous line drops readily.

It would be well to consider briefly the instantaneous voltage drop imposed on a welding station by power transformer and feeder reactance. Standardization has not yet fully clarified the meaning of percentage voltage regulation for phase-controlled welding machines caused by this effect. From Figure 4C it should become apparent that with the form of voltage wave shape shown, the use of a voltmeter to register welding load voltage is inaccurate. The cathode-ray oscilloscope offers a better means of obtaining the voltage drop. The question then arises as to where to measure the voltage drop when speaking in terms of percentages. One criterion might be to consider the regulation as:

% instantaneous regulation =

$$\frac{e_d}{e_L} \text{ at line-voltage peak (90 degrees)} \qquad (1)$$

Here e_d and e_L are initial line drop and load voltage respectively. Also:

$$e_d = L\,di/dt \text{ for } i_0 \qquad (2)$$

where L = lumped power source inductance

In considering over-all welding regulation, it is necessary to discard the no-load voltage area C shown in Figure 4C. Regulation for the half cycle shown then becomes:

$$\%\text{ total regulation} = \frac{\text{area } B+B'}{A+A'+B+B'} \qquad (3)$$

The voltmeter could measure this accurately for one condition, namely, full sine wave welding current, where an instantaneous drop does not normally occur. Strictly speaking, equations 1 and 3 must be reconsidered each time another fractional heat current is used. The reason for this is that partial heat currents are nonsinusoidal and have initial rates of rise that depend on the ignition point, that is, the controlled firing angle. The welding power factor also contributes to the degree of the instantaneous voltage drop. Thus the importance of instantaneous voltage drop cannot be overemphasized.

The detection circuit of the voltage compensator was designed to take into account this complication of measuring just such voltage regulation. The rectifier supply, which is in effect the detection circuit, has an inductive input filter which integrates the voltage wave, so that instantaneous drops that occur after 90 degrees will readily be averaged into the resultant output voltage.

A test was conducted using actual weld pull specimens and dropping the voltage by means of another timer operated simultaneously. The results with and without compensation are shown in Table I. Note that the average for 0.016-inch stainless steel with compensation is 399 pounds pull strength, whereas when uncompensated the average fell to 333 pounds strength because of external line drop. An ampere-squared second recorder indicated that approximately only 70 per cent heat was getting to the weld when uncompensated.[3] The difference is even more marked for 0.050-inch stainless steel, shown in Table II. Here the average fell from 2,634 pounds pull when compensated down to 2,063 pounds pull when not compensated for the particular line drop. The recorder indicated approximately 76 per cent heat into weld for the uncompensated case. A definite advantage in favor of the compensator was found as a result of these tests.

The compensator was next tested under severe conditions and operated with an instantaneous voltage drop as high as 27 per cent. Without compensation the recorder indicated that the heat into weld was about 50 per cent of normal. The compensator was therefore able to bring the heat up to 100 per cent (entirely back to normal). This is strikingly illustrated by shear strength pull tests which showed perfect weld strength of 2,300 pounds when compensated but fell off to 1,350 pounds when uncompensated for the drop referred to. Such a test points to suitable compensation for several heavy welders operating simultaneously, provided the power source is not being too severely overloaded. The main purpose of this test was to determine evenness of compensation under the wide variety of conditions mentioned earlier. With

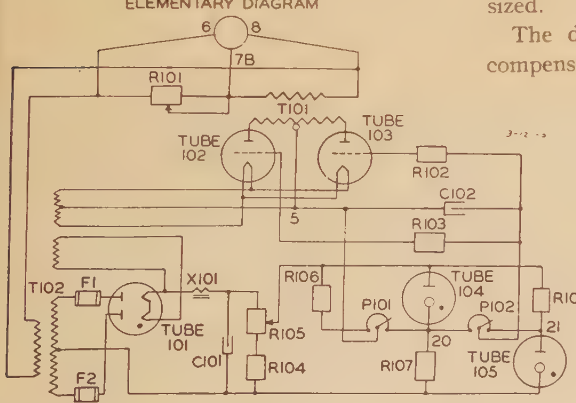


Figure 5 (left). Elementary diagram of compensator panel

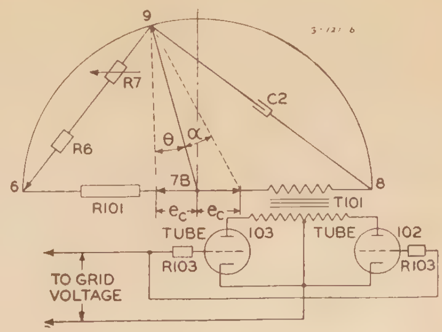


Figure 6 (right). Vector sketch showing how phase of peaked firing voltage is shifted by compensation

Heat control circuit is included. e_c is compensation voltage

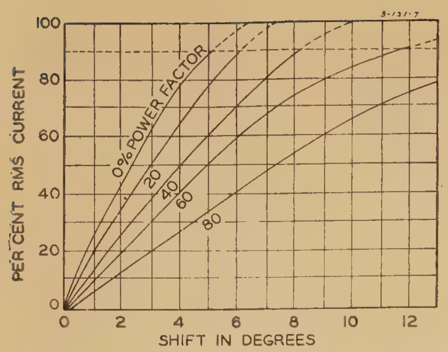


Figure 7. Phase shift required to maintain constant current with ten per cent line-voltage drop

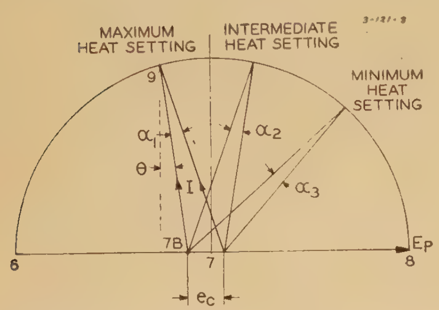


Figure 8. Vector sketch showing heat control range and point 7B shifted as required

various combinations of heavy and light loading for operation of both timers, deviations of the recorder for the most part hovered well within five per cent of normal. Adjustment of the compensator was not changed during this test. The greatest deviation experienced was over compensation of 15 per cent with the extremely heavy external drop referred to and then only for very low heat settings of the compensated timer. Even this is within tolerance limits and is remedied by reducing the power-factor-gain control slightly, if machine is operated continuously on this range. The power factor used was 0.6, which is fairly high, and the time was eight cycles. Consistent results were also obtained on lower power factors.

Oscillograms were taken to illustrate compensator action. In order to provide the line drop, a second timer known as a line dropping timer was operated on the same line. Referring to Figure 9, the two timers came on the line together, and the compensator-test timer had the compensator-gain-control knob $P102$ turned off. Thus, the test timer operated without compensation and when the dropping timer C stopped, the difference in the magnitude of weld current A is apparent.

Figure 10 illustrates normal compensation. The compensator acted fully on the weld current A within two cycles from the initial cycle dropping the line. After the external drop went off, the compensator required two cycles to phase back to normal, as seen by the amplitude of cycles 5 and 6 which are above normal. Since these oscillograms were taken, the speed of operation has been reduced to one cycle to yield full compensation. Notice the instantaneous drop expressed by the line voltage B. This same voltage drop (Ldi/dt) is experienced by every control transformer and circuit in the control panel and is seen by the dip of $T101$ (primary) voltage D of compensator and its related branch $R101$ shown as voltage E (reduced for clarity).

A better comparison is obtained from Figures 11 and 12. Figure 11 is without compensation and illustrates the current decrease from a normal rms value of 750 amperes, as read on the primary side of welding transformer. Figure 12 shows how this decrease was made up by compensation. Primary weld current A at cycle 2 became lower from an external load, but cycle 3 is almost up to normal compensation. Cycles 4 and 5 (and all further succeeding compensated cycles) appear normal. However, after release from the external load, cycles 6 and 7 continued to respond to phase advance. This is not particularly a disadvantage, as it tends to make up for the early partially compensated cycles (2 and 3). Cycles 8, 9, and 10 duplicate the initial cycle. It can be noticed that the shoulders, or phase concurrence for cycles 8, 9, and 10 are later than for compensated waves 3 to 7 inclusive. This accords with the principle of compensation employed. Were the oscillograph element A as responsive as element C, these shoulders would have the same flatness. Of importance, the peak amplitude of compensated current loops 4 and 5 need not equal normal loops 8, 9, and 10, since the criterion for equal compensation is that the rms value of the loop areas be equal in both cases. The instantaneous values of D and

Table I. Sample Shear Pull Strength Tests for Type 304 Stainless Steel 0.016 Inch Thick

Line Condition During Weld	I^2T Recorder in Per Cent of Normal Heat: With Compensator	I^2T Recorder in Per Cent of Normal Heat: Without	Corresponding Weld Pull (Pounds): With Compensator	Corresponding Weld Pull (Pounds): Without
Compensated weld machine. No line drop (start of test)	100 (Reference)	100 (Reference)	380	397
Compensated weld machine. No line drop (finish of test)	100 (Reference)	100 (Reference)	390	400
External line drop	95	71	418	332
External line drop	100	70	380	342
External line drop	100	70	400	328
External line drop	102	71	412	325
External line drop	100	72	385	340
External line drop	97	73	370	350
External line drop	100	71	375	322
External line drop	101	72	448	320
Average	99.3	71.3	398	332

Type 304 stainless steel 0.016 inch thick—1/4 hard.

Weld time: four cycles. Weld current: 70 per cent (fractional). Electrode size: 1/4 inch. Electrode force: 340 pounds total. Line: 240 volts a-c. Regulation: six per cent. (Read with voltmeter during welding current, but using longer time.)

Regulation for instantaneous external line drop: 10 per cent. (Read with oscilloscope for partial current firing at approximately 100 degrees on voltage wave.)

Figure 9. Oscillogram of timer performance without compensation

A. Primary current of welding machine with installed compensator

B. Line voltage (240 volts a-c) common to both welding machines

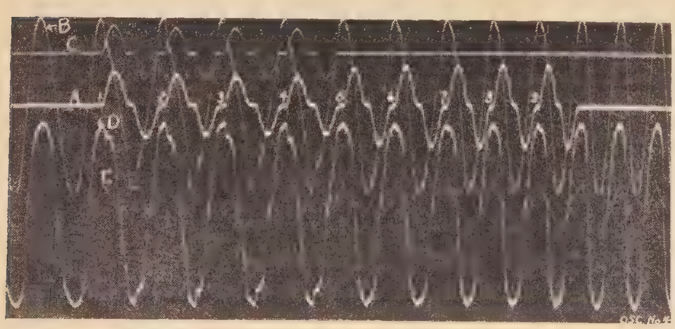
C. Primary current of line-voltage dropping time

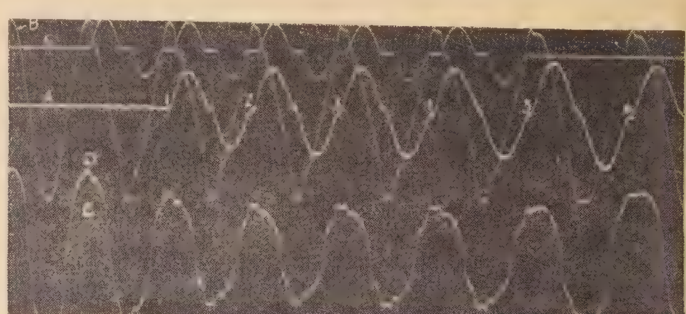
D. Voltage across *T*101 transformer primary winding in compensator. Adjusted value 260-volts a-c rms

E. Voltage across *R*101 bridge resistor in compensator. (Reduced in size for clarity.) Adjusted value 220 volts a-c rms

Figure 10. Oscillogram of welding machine performance with compensation. Early line drop

See subcaption to Figure 9





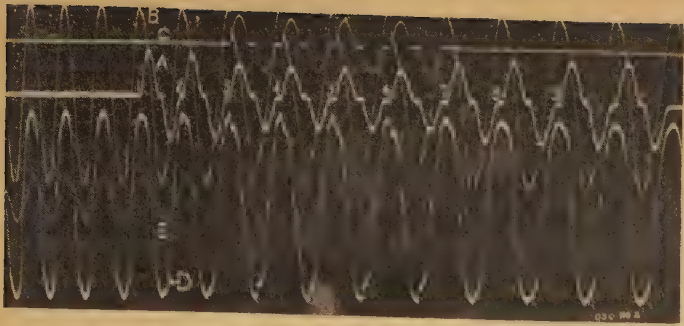


Figure 11. Oscillogram of welding machine performance without compensation. Late line drop

See subcaption to Figure 9

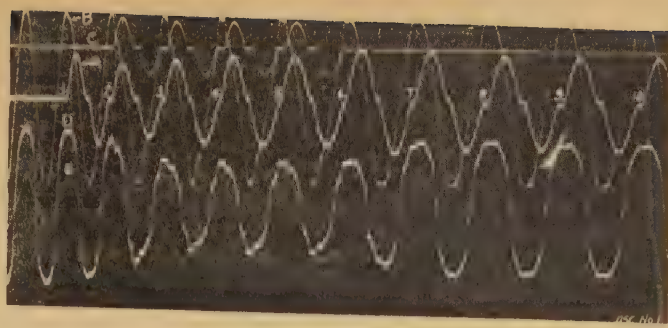


Figure 12. Oscillogram of welding machine performance with compensation. Late line drop

See subcaption to Figure 9

(b). A slight unbalance of voltages across bridge arms $R101$ and $T101$ to allow uniform response in installations where the welder power factor is much higher than normally experienced. All adjustments are quickly performed, using a combination voltage test set.

E found in the oscillogram determine the shift of midpoint 7 of Figure 7. Voltage D corresponds to $T101$ voltage and E to $R101$, and their shift toward the right for compensation upward agrees with previous discussion. The I^2T recorder read 100 per cent normal heat for the oscillograms being compensated and dropped more than 15 per cent for those which were uncompensated, thereby causing lockout of the welding machine.

Although data were not taken for slow drifts in the sine wave value of line voltage, the response on seam welders using thermocouple ammeter indication has shown that the compensator handles such changes readily.

Investigating Compensator Performance

Several methods of investigating performance are available.

1. The cathode-ray oscilloscope, when connected to read one full cycle of weld current, will indicate the phase-angle advance during compensation. The oscilloscope is also helpful for making electronic control-panel power-factor heat adjustment after installing a compensator.

2. The I^2t recorder, illustrated in Figure 4, is well adapted to record changes in welding current. Where this recorder is available, a quick test is performed by operating two welding machines on the same line individually and together. Uniform recorder response, when the control panel is set to various heat positions, does indicate correct compensation where the regulation would otherwise drop the recorder below the 15 per cent heat limit and cause bad welding.

3. Spot-welding machines may be tested with either clamp-on pointer stop ammeter or pointer stop ammeters in conjunction with current transformers. The previous method for operating two welding machines together is recommended.

4. Seam welding machines may take advantage of a suitable ammeter such as a thermocouple type with an adjustable primary current transformer for indication of operation. Adjustment of the compensator is readily accomplished here, provided a definite line disturbance can be initiated during the test.

5. Where none of the aforementioned methods is available, the compensator can be adjusted and its operation checked by using merely a voltmeter across the control signal bridge and also across the primary winding of $T101$ for visually checking voltage swings. Normal adjustments once made need not be disturbed following one of these performance checks. The only special adjustments are

(a). The degree of compensation desired (same for all normal operation).

Table II. Sample Shear Pull Strength Tests for Type 304 Stainless Steel 0.050 Inch Thick

Line Condition During Weld	I²T Recorder in Per Cent of Normal Heat: With Compensator	I²T Recorder in Per Cent of Normal Heat: Without	Corresponding Weld Pull (Pounds): With Compensator	Corresponding Weld Pull (Pounds): Without
Compensated weld machine. No line drop (start of test)	100 (Reference)	100 (Reference)	2,700	2,800
Compensated weld machine. No line drop (finish of test)	100 (Reference)	100 (Reference)	2,600	3,100
External line drop	96.5	82	2,725	2,200
External line drop	97	83	2,725	2,400
External line drop	105	70	2,600	1,900
External line drop	100	75	2,625	2,000
External line drop	100	73	2,500	1,800
External line drop	95	73	2,600	1,800
External line drop	95	72	2,700	1,850
External line drop	100	76	2,750	2,100
Average	98.6	75.5	2,634	2,063

Type 304 stainless steel 0.050 inch thick—1/4 hard.

Weld time: nine cycles. Weld current: 75 per cent (fractional). Electrode size: 3/8 inch Electrode force: 1,200 pounds total. Line: 240 volts a-c. Regulation: 10 per cent. (Read with voltmeter during welding current, but using longer time.)

Regulation for instantaneous external line drop 15 per cent. (Read with oscilloscope for partial current firing at approximately 90 degrees on voltage wave.)

Conclusions

1. This voltage compensator helps to maintain uniform current for resistance welding despite the disturbing influence of other machines on the line feeder. With wartime urgency of using existing feeders and with limited use of copper, the problem of poor voltage regulation is eased considerably.

2. Instantaneous reactance line drops (chopped voltage waves) in the order of 30 per cent have been compensated for 100 per cent. Without compensation the heat output was otherwise 50 per cent of normal value.

3. Another feature is compensation for upward or downward line-voltage shifts during the working day.

4. The ability to make consistent welds on poorly regulated lines, is, of course, the chief advantage of the compensator. On installations where the weld recorder locks out the equipment when the value of I^2T swings out of the allowable range, the operator is forced to reset the welding machine and usually to make new weld tests and rewelds. The voltage compensator eliminates to a large degree the number of such lockouts caused by variations in current with a resulting saving in lost time

5. The unit is inexpensive, simple, compact, easy to install and adjust. It is entirely automatic, since the response was designed for uniform compensation for any fractional heat current settings.

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Thyratron Motor Control

E. E. MOYER
NONMEMBER AIEE

H. L. PALMER
ASSOCIATE AIEE

Synopsis: The control of d-c motors was one of the first electronic-control projects. Recent developments, particularly the fully automatic and co-ordinated control of the armature power circuit and the field power circuit, have greatly increased the acceptability of these controls for industrial use.

This paper describes the control functions associated with an all-electronic d-c motor drive, in which grid-controlled thyratron-tube rectifiers automatically perform the variable voltage functions associated with the d-c generator, exciter, and field rheostats of a conventional motor-generator drive.

In addition, the grid-control action of the thyratron-tube rectifier dispenses with the usual magnetic starting and accelerating control devices and gives superior motor performance in the matter of almost idealized constant-current acceleration and flat preset speed-control characteristics resulting from *IR*-drop compensation and regulated field excitation.

Reversing is accomplished by magnetic reversing switches which reverse the armature terminals relative to the rectifier. Contact duty during switching is minimized by the supplementary action of the thyratron tubes which themselves serve to interrupt and initiate the armature current. Proper grid-control sequence in these same tubes causes them to act as inverters during the decelerating portion of the reversing cycle so that the rotational energy of the armature is actually pumped back into the a-c system.

Stopping is normally by means of a dynamic braking resistor, although the motor may be decelerated to a stop by utilizing the inverter action associated with reversing to regenerate power back into the a-c system.

Similar control schemes are applicable to motor-generator-set drives to control the generator and the motor fields in response to co-ordinated signals of motor speed and loop currents.

THE basic problem of realizing the flexibility of a d-c motor where only a-c power is available can be readily solved by the use of thyratron-tube rectifiers. The source of direct current is obtained from the rectifier, and the grid-control feature offers a means of controlling the motor by low-energy grid circuits.

The control can make the motor perform in practically any way desired. The speed can be controlled in accordance with requirements of the machine it drives, such as holding tension in a wire-reeling machine or holding loops between the stands of a rubber, cloth, paper, or steel mill. The speed can be held constant over a wide range of load or line-voltage conditions. The torque output can be held substantially constant from zero to base speed. Protection during acceleration and on overload is possible by means of current limit built into the control.

The rating of the rectifier, the type of circuit used, and the rating of the individual power tubes determine the size of motor which can be so controlled. In the case of larger size motors, the output wave shape is more of a determining factor on the circuit used than the size of the motor. The rectifier circuits used conform to standard rectifier practice and will not be covered in detail in this paper. The conventional biphase half-wave circuit controlling a one-horsepower 230-volt d-c motor will be used as the example in describing the theory of operation. It will be noted that two rectifiers are required, one for the armature power and one for the field. When the motor is to be operated from base speed down, by varying the armature voltage, the field rectifier is uncontrolled. A variable-speed motor can be made to cover a wide speed range by using a controlled rectifier on the field as well as on the armature.

The armature rectifier consists of grid-controlled thyratrons, and the output voltage is varied by shifting the phase of the grid voltage of the thyratrons with respect to their anode voltage. There are several ways of shifting the grid voltage, but the one used in these controls is the conventional inductance–resistance bridge. The inductance will be the variable element by the use of a saturable reactor. In this way the output of the rectifier can be controlled by the small d-c current in the saturating winding of the saturable reactor. Such a circuit is shown in Figure 1.

Auxiliary D-C Control Power and Voltage Standards

Since direct current is required for these saturating windings, a source is provided by a small thermionic rectifier and anode supply as shown in Figure 2. The d-c voltage is filtered by means of a reactor *X*1 and capacitor *C*1 and fed through a current-limiting resistor *R*1 and the voltage-regulating tubes *A* and *B*.

Tube *B* is primarily used as a constant voltage reference against which various signal voltages are compared. It is an inherent characteristic of a glow tube that its terminal voltage will remain essentially constant at rated value even though the current through the tube changes widely. The tubes used in this circuit maintain approximately 75 volts, and it is by this means that a reference voltage for regulating purposes is established that will remain relatively constant irrespective of a-c line-voltage changes.

Tube *A* is connected in series with tube *B* to give a constant potential three-wire d-c system of 150/75 volts for the plate and grid voltages of the control tubes.

The d-c winding of a saturable reactor is connected in series with a triode vacuum tube *C*, and this combination is connected across the 75-volt d-c control bus furnished by regulator tube *A*. (See Figure 3A.) With a negative voltage on the grid of tube *C* there will not be any plate current, the reactor will be unsaturated, and the thyratrons are phased off. If the grid is made less negative, tube *C* will pass some current, and the reactor will be partially saturated, which will advance the phase of the grid voltage. As the grid voltage of tube *C* approaches zero, the reactor will be completely saturated, and the thyratrons will be full on.

Armature-Voltage Control

The control scheme consists of comparing a portion of the armature voltage with a preselected portion of the voltage-regulator tube's voltage. The difference between these two voltages is amplified and applied to the grid of the triode, which saturates the reactor controlling the grid phase shift on the thyratrons supplying the armature of the motor.

To couple the armature voltage to the triode which saturates the reactor, it is necessary to have a common point for the signal voltage from the armature and for the grid voltage applied to the triode. It is also necessary that the relation of these voltages be correct; that is, an increase in armature voltage above a preset voltage level should make the grid of tube *C* more negative so as to retard the phase of the thyratron grid voltage. By studying Figure 3B, it will be apparent

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E. E. Moyer and H. L. Palmer are in the electronics section, industrial control division of the General Electric Company, Schenectady, N. Y.

that the voltage relations are incorrect and, if directly connected, would give incorrect operation. It is therefore necessary to change this relation.

This can be done as shown in Figure 3C as follows: The grid of tube C is connected to a voltage divider across the 150-volt control bus so that the grid is positive when tube D is passing very little or no current. Then, as more current is caused to flow through tube D, the grid of tube C will be pulled negative by the increased current through $R2$. It is now possible to control the phase of the thyratron grid voltage by changing the voltage on the grid of tube D with the relation correct for regulation of the armature voltage.

Armature-Speed-Control Potentiometer

With the cathode of tube D connected to the mid-point of a voltage divider across glow tube B, as shown in Figure 3C, the cathode will be $37^1/_2$ volts positive with respect to the negative bus. With zero voltage on the armature, the grid of tube D will be $37^1/_2$ volts negative, and the thyratrons will be in a condition to conduct full half cycles. As voltage on the armature builds up to nearly $37^1/_2$ volts, the thyratrons will shut off just enough to maintain a balance between the armature voltage and the $37^1/_2$-volt reference.

In order to permit the adjustment of the armature voltage or speed, resistor $R5$ is replaced with a potentiometer so that the cathode potential of tube D can be changed. In order to permit the use of 230 volts on the armature of the motor, a voltage divider is connected across the armature and a portion of the armature voltage compared to the reference voltage. This is shown in Figure 4. The potentiometer shown in the armature voltage divider gives a means of setting the base speed with the speed control set for maximum speed. This adjustment gives a means of compensating for commercial tolerances in resistors and tubes.

IR-Drop Compensation

Armature speed is proportional to armature terminal voltage only when the armature is drawing no current and is spinning freely with the field excited. Under these conditions, the armature voltage is the counter electromotive force or generated voltage of the armature conductors. When current flows in the armature, as when the motor shaft is loaded, the counter electromotive force and hence the true speed is less than the terminal voltage by an amount equal to the voltage drop in the resistance of the armature conductors themselves. If a constant shaft speed is to be maintained irrespective of load, it is necessary to raise the terminal voltage progressively as the load current increases, by an amount equal to the armature-resistance voltage drop. To do this it is only necessary to introduce in the armature-control circuit a voltage proportional to the armature current in such a direction as to hold a higher armature voltage as the load on the motor increases.

Although it would be possible to build an amplifier system that would amplify the millivolt drop of a conventional shunt to such a point that it could be used, it offers serious difficulties. In order to get a more suitable voltage, a special current transformer is designed to operate in the anode leads of the rectifier tubes. The pulses of current through these anode leads are fed into the two primary windings so as to act as an alternating current that bears a direct relation to the direct current. The secondary winding of this current transformer is loaded by a resistor and a full-wave rectifier, and the resultant d-c voltage is directly proportional to the armature current. The value of this voltage for a given current can be adjusted with a potentiometer.

This d-c voltage that is proportional to armature current is connected in the lower end of the armature-voltage-divider circuit as shown in Figure 5. This added voltage in effect subtracts from that part of the armature voltage used as a feedback and therefore causes a higher voltage to be maintained by the regulating action. The adjustment provided permits setting the control for only partial compensation, or it can be overcompounded.

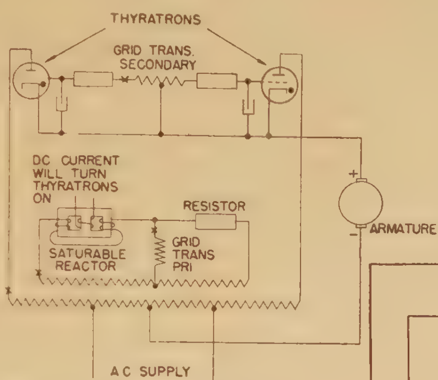


Figure 1. Grid-controlled rectifier with phase-shift bridge

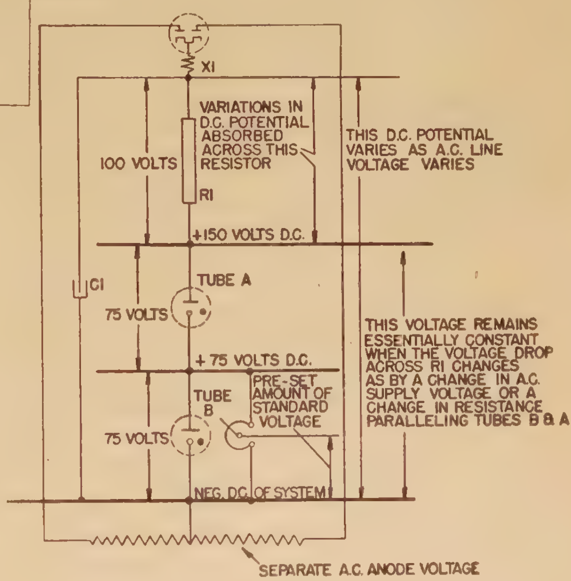


Figure 2. Regulated rectifier for control amplifiers

Figure 5A shows the effectiveness of the IR-drop compensation. The dotted curves represent the normal uncompensated motor-regulation characteristics. The solid curves were taken with the IR-drop compensation set for flat compounding at 100 per cent rated speed. Note the great improvement in the motor characteristics at the lower speeds.

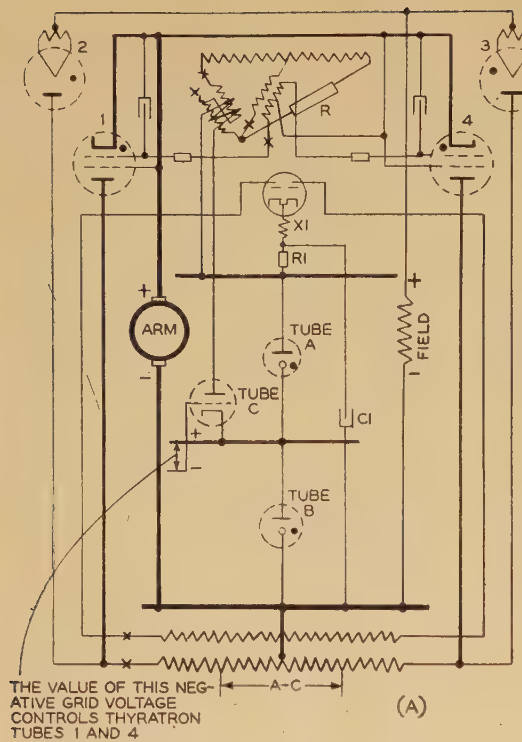
Current Limit

Current limit is necessary to prevent excessive currents being drawn during acceleration so as to protect the tubes and keep within the limits of commutation of the motor; otherwise, overloads could occur by suddenly applied shaft loads or by starting with a preset speed adjustment. A current-limit control should be inoperative from zero current up to the preselected value and then prevent the current rising above this value. If this were not the case, the current would be influencing the speed; instead, it should take over sharply as near the preset current value as possible.

A d-c voltage proportional to armature current has been established for IR-drop compensation, and this same voltage can be used for current limit. A current-limit signal is established by comparing a portion of the d-c voltage that is proportional to current with a known voltage

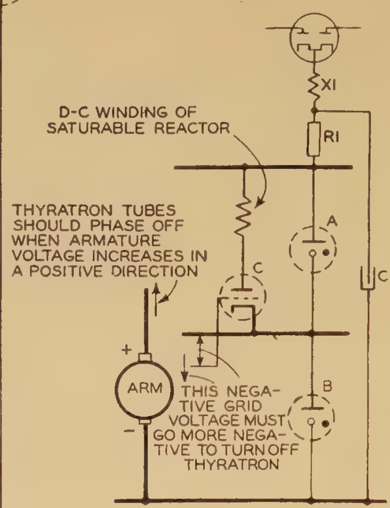
standard and amplifying the difference by a triode. The circuit showing the connections for this control added to the armature control and IR-drop compensation is shown in Figure 6.

The value at which the current limit takes over is adjustable by means of the current-limit potentiometer. As long as the voltage on the slider is less than 75 volts, the current-limit tube E will be biased to cutoff, or practically so, and the armature-control tube D will have complete control. If high armature current causes the voltage on the current-limit-potentiometer slider to reach 75 volts, tube E will pass full anode current which will retard the grid voltage phase on the thyratrons. This reduces the current, and a balance will be reached. When the load on the motor is reduced and the armature current goes below the preset value, the armature-control tube D will take over and hold armature voltage as long as the current stays below the current-limit point.



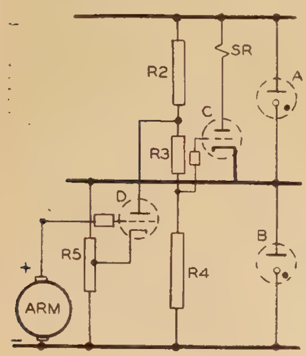
(A)

A (left). Variable d-c source for saturable reactor



(B)

B (above). Showing that armature voltage has incorrect sense to be coupled directly to grid of tube C



(C)

C (left). Showing that armature voltage now has correct sense to be coupled directly to tube D

If armature voltage tends to increase (becomes more positive), the grid of tube D becomes less negative and tube D turns on which turns tube C off which reduces saturation of SR which retards grid phase and reduces thyratron output

Figure 3. Armature-voltage-regulation circuit

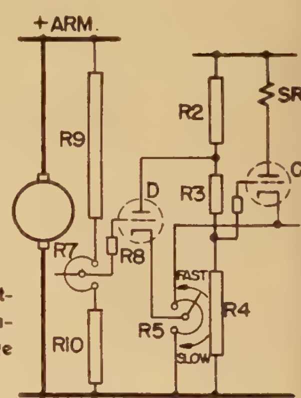
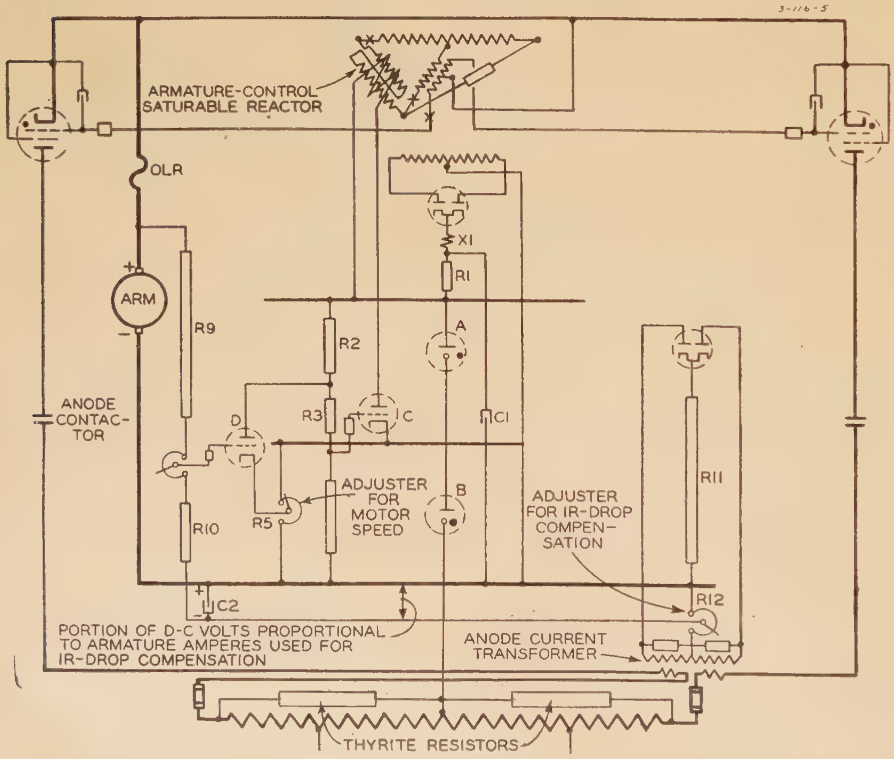


Figure 4. Armature-voltage-regulating circuit complete with armature-voltage divider

Figure 5 (below). Armature-voltage-regulation circuit with IR-drop compensation added



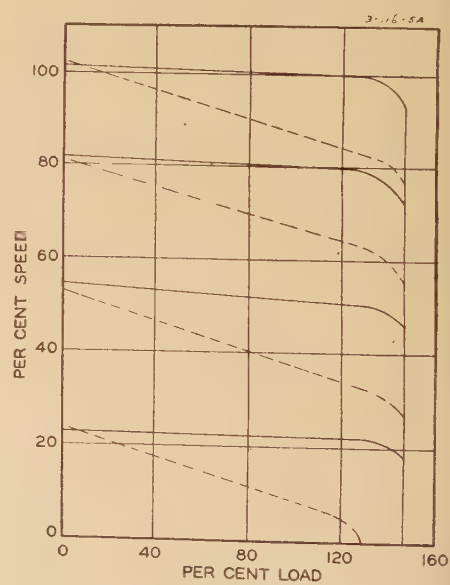


Figure 5A. Speed-regulation curves showing effect of IR-drop compensation

Dotted regulation curves are without IR-drop compensation. Solid regulation curves are with IR-drop compensation

With this type of control it is possible to accelerate from zero to full speed without drawing more than the preset value of current through the armature. During the operation of the motor, if excess load is applied, the speed will be maintained up to the point where the current reaches the current-limit setting. Beyond this point the preset value of current will be maintained in the armature even down to the stalled armature condition. The current will be maintained until the thermal overload relay functions.

When starting from rest with zero current or when reversing, there is no current to generate a current-limit voltage, and the armature voltage is zero; therefore, the first few half cycles of current might be of destructive magnitude. To overcome this objection, a normally closed interlock on the initiating contactor is used to give a false signal in the current-limit circuit. By means of a capacitor this false signal is "remembered" for a time after the circuit is closed and makes the current build up to the current-limit value rather than have one large surge of current and come down to the correct value.

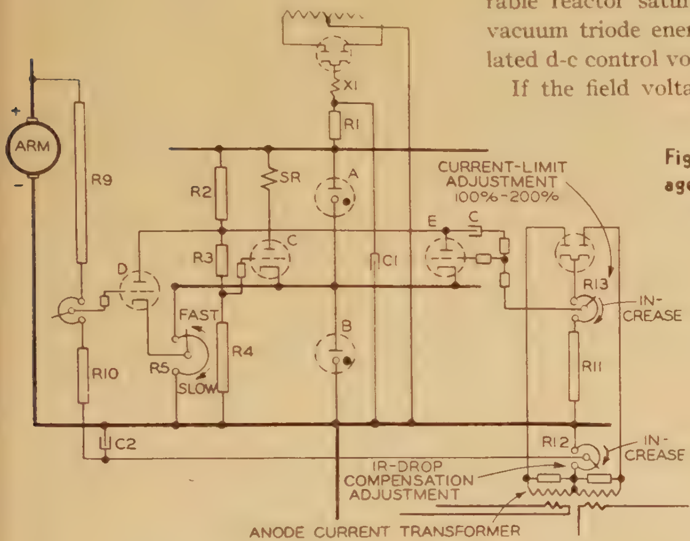


Figure 6. Armature-voltage-regulation circuit with current limit added

Stopping

The rectifier characteristics of the thyratrons supplying direct current to the motor prevent the reversal of power from the motor to the a-c system when the motor is running faster than the speed called for by the control. This condition exists, for example, when the speed-control potentiometer is suddenly turned to a lower speed. In the case of high inertia loads this "electrical free-wheeling" characteristic is objectionable, but on friction loads it is of no consequence.

When power is removed for stopping, the same system of dynamic braking can be used as is found on magnetic control; that is, a resistor may be connected across the armature to absorb the stored energy. If quick slowdown is required, it is possible to add a control tube and a relay that will actuate the dynamic braking contactor to connect a resistor across the armature when the motor speed is above that called for by the control. When the speed is reduced to the called-for value, the tube initiates the function of disconnecting the resistor.

Field-Weakening Control

In order to expand the range of speed control, the shunt field rectifier tubes can be replaced with thyratrons and control added to the field. The control of the field rectifier is accomplished by using another saturable reactor and resistor bridge with the d-c winding of the saturable reactor saturated through a high vacuum triode energized from the regulated d-c control voltage.

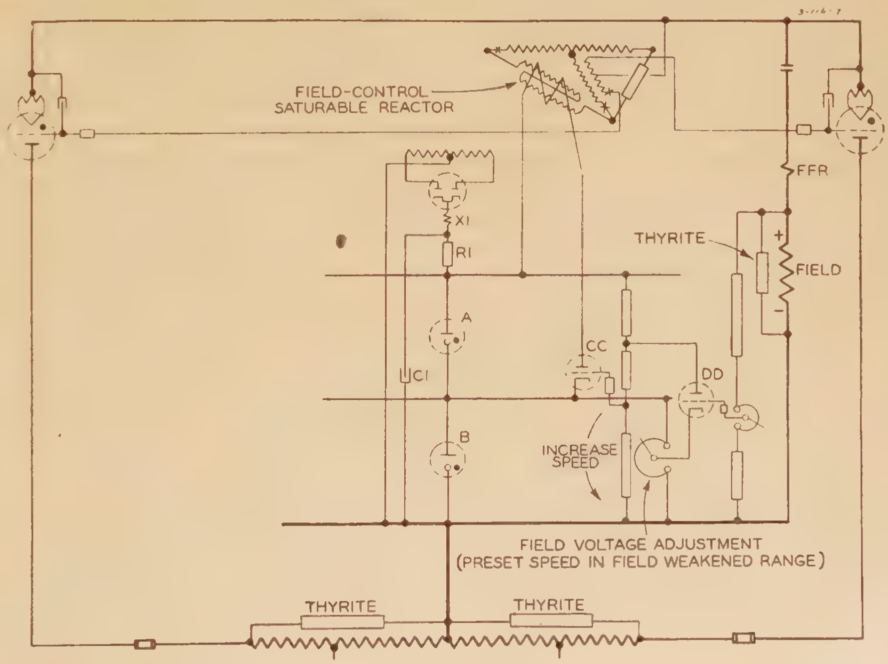
If the field voltage is accepted as an indication of field flux, the control system can be similar to the armature-control circuit in that a voltage-regulating system is used. This regulating circuit is shown in Figure 7. The armature controls are not shown, to avoid confusion. The electric feedback of the field-voltage signal will maintain a preset level of field excitation independent of line-voltage variations.

Where the motor speed is being controlled by varying the armature voltage, the field excitation should remain at rated value to develop rated torque. Conversely, when operating the motor in the weak field range, the armature voltage should remain at rated value to develop constant horsepower. This characteristic can be had by ganging both the armature-control potentiometer and the field-control potentiometer on the same shaft. To do this each control is compressed into half the rotation, with the other half a conducting segment of negligible resistance. These are staggered so that with the armature-control slider in its active range the field-control slider will be on the conducting segment holding full field.

Speed Regulation at Reduced Field Excitation

When the field excitation is reduced the ratio between speed and armature counterelectromotive force is changed such that the armature must rotate at a higher speed in a weaker field in order to generate a given electromotive force. A given preset speed level is maintained relatively constant by grid phase control of the armature thyratrons as they phase

Figure 7 (below). Field rectifier with voltage control



advance to compensate for IR drop accompanying changes in armature current.

In this system speed is not regulated by changes in field excitation. Speed is only preset to new levels by the field control but is regulated by armature control. When the armature voltage has been increased to the limit determined by the anode voltage, there can be no further regulatory action.

Field Excitation During Acceleration

It is necessary to apply full excitation to the motor field in order to accelerate it from standstill to base speed. Above base speed the field should be weakened slowly enough to keep the armature current within the commutating ability of the motor. Therefore, the application of armature voltage and the weakening of the field must be controlled by the current-limit circuit. By doing this, the motor is made to accelerate at the maximum possible rate permissible without drawing excessive current. Full field during acceleration from zero to base speed is maintained regardless of the setting of the field-voltage control.

The circuit that accomplishes the aforementioned features is shown in Figure 8. It is similar to the current-limit control on the armature voltage shown in Figure 6 in that the d-c voltage proportional to armature current is compared with a standard voltage, and the difference voltage is applied to a triode. The triode acts on the saturating circuit of the grid phase shifting reactor of the field rectifier.

There are two differences between the armature and the field control. First, an increase in armature current beyond a predetermined amount must increase the field strength by saturating the saturable reactor. When controlling the armature voltage, an increase in armature current necessitates a decrease in the saturation of the armature-control saturable reactor. Second, the standard voltage is not directly the constant voltage of regulator tube *B* but is the slightly variable voltage between the grid of tube *CC* and the negative bus, which voltage is in turn referred to the standard voltage of tube *B*.

The field forcing triode tube *EE* is connected so that, when it is caused to conduct by high armature current, the grid of tube *CC* will be pulled positive. The increased current in the anode of tube *CC* will increase the saturation of the field-control saturable reactor and apply more field.

Figure 8. Voltage-regulated field control with armature-current limit added

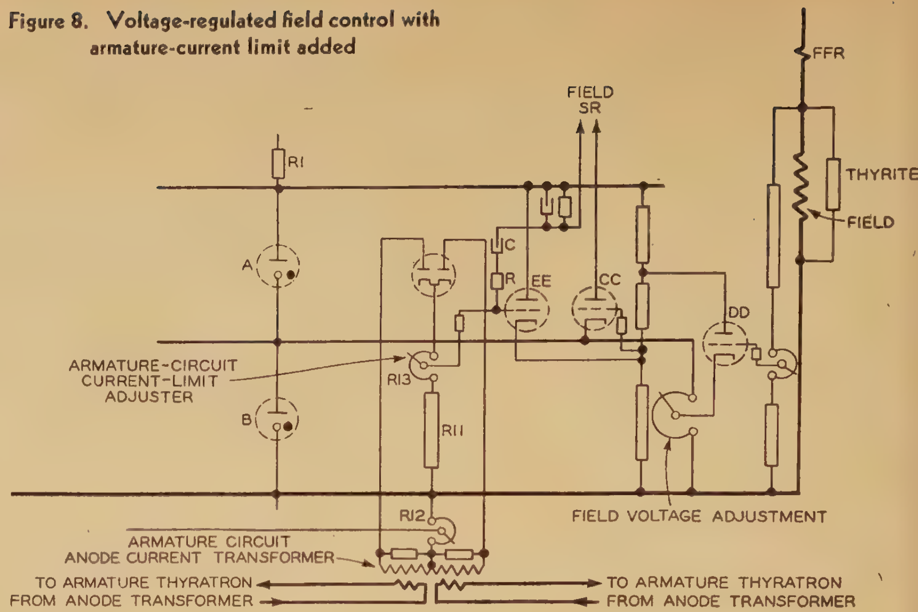


Figure 9. Armature-current-limit control connected to both the armature-voltage and field-voltage control

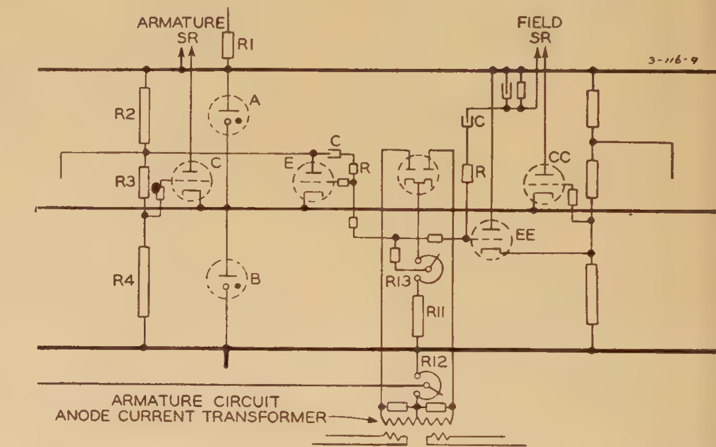
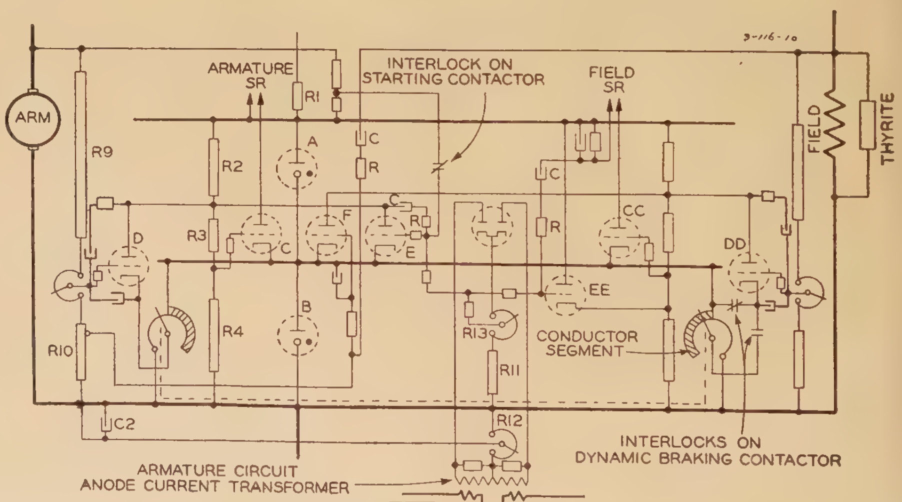


Figure 10 (below) Complete armature- and field-voltage controls



The normal operation of the circuit shown in Figure 8 will put the grid of tube *CC* and the cathode of tube *EE* slightly negative of the neutral bus, or almost 75 volts positive with respect to the negative bus. The grid of tube *EE* is connected to the negative bus except for the voltage proportional to armature current. This condition means that field forcing tube *EE* will be nonconducting until the armature current reaches a preset value determined by the setting of *R*13. Therefore, with the armature current below this preset value, the field regulator will operate normally, but tube *EE* will take over and increase the field as soon as the armature current exceeds the preset value.

The field forcing triode tube *EE* will act ahead of the armature current-limit

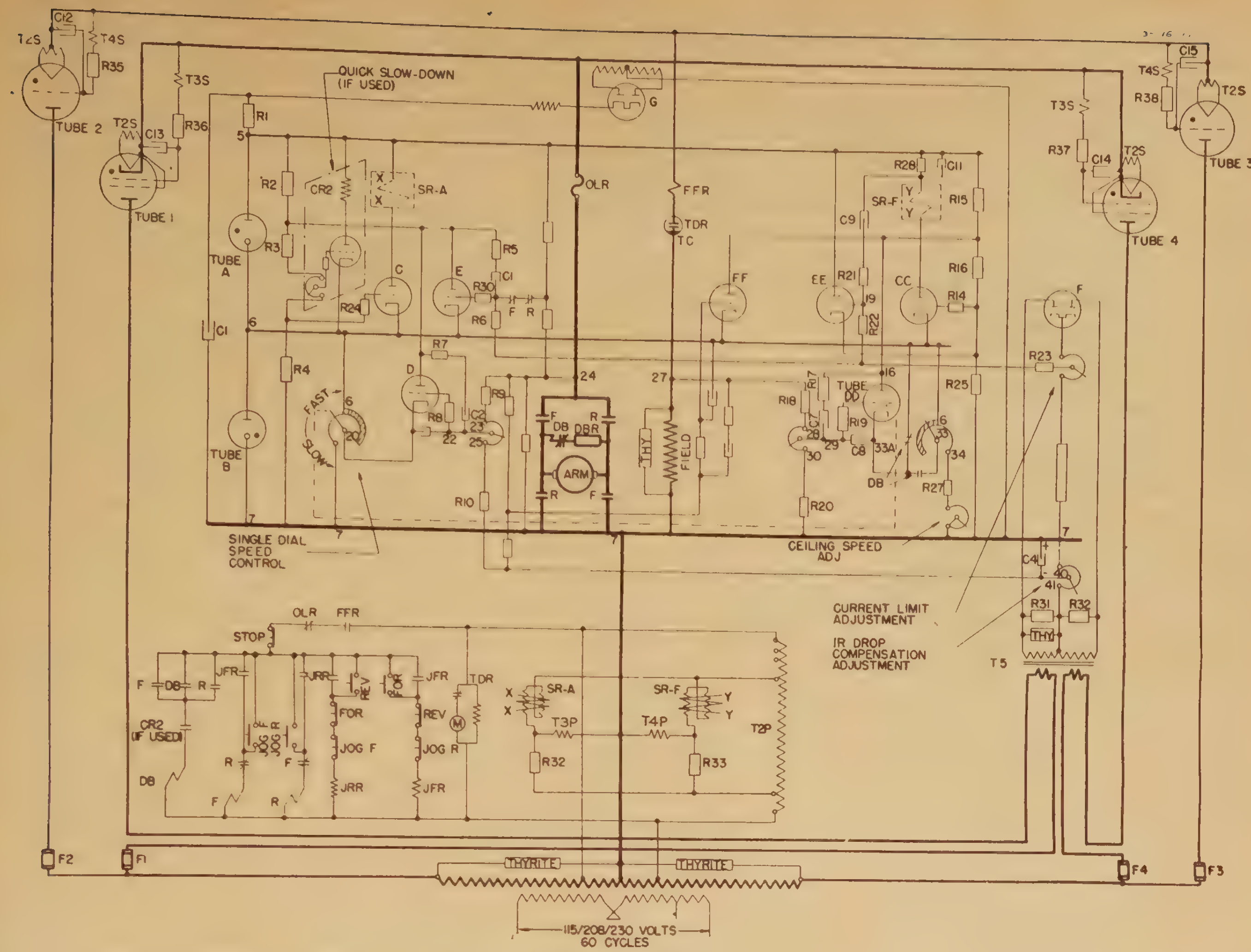


Figure 11. Complete power and control circuits of an electronic drive for a one-horsepower 230-volt d-c shunt motor

triode because the reference voltage of tube *EE* is slightly less than tube *E*. This is diagrammed in Figure 9 where both the field forcing and armature voltage control are shown connected to the same current-limit signal voltage. In the case of suddenly applied loads that do not give time for the field flux to build up to the needed value, the armature voltage will be reduced and prevent overloading the armature supply tubes. The two controls work independently and are arranged so that the field control works at a slightly lower value than the armature control.

The field circuit may be preconditioned to full field value prior to starting by means of interlocks on, say, the dynamic braking contactor. Then, when the starting is initiated, the false signal is removed, but its effect dies off more gradually because of capacitor discharge circuits and maintains full field until the armature current can take control via tube *EE*.

Armature-Voltage Limit When Decelerating

If the speed-control potentiometers are suddenly turned from the weak field condition to a speed calling for full field, the armature voltage will rise above the applied voltage. The voltage will rise because the full field would be applied suddenly and the motor becomes a generator. Because of the rectifying action of the thyratrons, current cannot flow from the machine into the line and dynamically brake the motor as it would be if connected directly to a d-c generator. This would be an objectionable condition, as it would endanger the insulation of the commutator and associated equipment.

This high generated voltage can be avoided by taking a signal from the armature voltage and actuating a triode that will retard the application of field when the voltage exceeds a preset amount. This preset value can be well above the operating value so that it will not interfere with normal control. This control is accomplished by tube *F* of Figure 10.

On nonreversing controls where the armature circuit is interrupted by an anode contactor instead of contacts at the armature terminals this armature-voltage-limit feature allows the use of a lower value of dynamic braking resistor than would normally be used on a motor being stopped from a weak field condition. The delayed field strengthening prevents excessively high armature currents when dynamic braking to rest by limiting the voltage across the armature.

Reversing Control

When the armature circuit is reversed by magnetic contactors between the output of the thyratrons and armature terminals, the duty on the contacts is much less than when the motor is supplied from a d-c generator. The duty on the contacts is reduced because the action of the thyratrons assists in interrupting the armature current. Because of the combination of inverter action and armtuure-current-limit control, the reversing cycle is such as to cause a uniform and rapid deceleration to zero speed, followed by acceleration to the preset speed in the reverse direction. During deceleration to zero speed, the rotational energy of the armature and the load is pumped back into the a-c system with the armature thyratrons. The preset speed in

The Effects of Mutual Induction Between Parallel Transmission Lines on Current Flow to Ground Faults

JOHN I. HOLBECK
MEMBER AIEE

MARTIN J. LANTZ
NONMEMBER AIEE

THIS paper is intended to present some of the problems encountered in determining unbalanced fault currents and unusual results that may be expected on systems with parallel lines on a common right of way. Correct results are required so that proper co-ordination and application of relays may be accomplished. Fault studies must be made and constantly revised, as a power system changes and expansions take place. Also, future conditions must be studied so that intelligent selection may be made of circuit breakers and current transformers. It is the purpose of this paper to discuss

1. The use of the a-c network analyzer in solving fault problems by using certain mathematical simplifications which are necessary in order to adapt the problem to the analyzer's limitations.

2. Some rather unusual conditions which may occur on a system during fault conditions because of the effect of mutual induction between parallel lines.

Frequently two or more power transmission lines are built on the same right of way parallel to each other for partial distances or for the full length. The problem of mutual induction between such circuits arises when unbalanced faults occur on one line causing induced voltages in the parallel circuit. Where the lines are of the same voltage and bussed at either or both ends, the calculation resolves itself into the use of a simple equivalent circuit. The problem becomes more complicated when the lines are operated at different voltages which obviously cannot be bussed at one end, or when a fault calculation is required on one end of a pair of parallel lines whose opposite ends are unbussed, such as a split-bus arrangement. In solving a problem of the latter type, use is made of the a-c network analyzer which utilizes one-to-one ratio transformers to represent the mutual induction between lines.

Usually fault-current calculations are within the scope of a d-c calculating board. As a power system becomes more complicated and more than one line occupies the same right of way, in some cases operating at different voltages, the effect of mutual induction between the lines becomes important for unbalanced faults. A d-c board cannot be used for these more complicated conditions.

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JOHN I. HOLBECK and MARTIN J. LANTZ are with the Bonneville Power Administration, Portland, Oreg. Mr. Holbeck is chief in the protection engineering unit, engineering division.

Zero-Sequence Currents

Since the flow of zero-sequence current is not restricted to the physical conductor but must return through the ground circuit, the resistivity of the ground itself enters into the determination of its impedance. The effect of the ground return circuit upon the impedance can be determined by either one of two ways; namely, by direct measurement after the line is constructed, or by assuming an average value of ground resistivity for calculating the impedance by formula. The latter method is the one which must be used in most cases (see appendix). It is not deemed within the scope of this paper to discuss the various theories of current flow through the ground but to show how assumed conditions are used to obtain what are considered reasonable results.

The effect of mutual coupling or the resultant induction between parallel circuits is inversely proportional to the spacing between the circuits and directly proportional to the length of parallel.

Mutual Induction

When two transmission lines parallel each other close enough for the mutual induction to be appreciable, there is transformer action between the circuits when unbalanced currents flow. The mutual induction resulting from the flow of positive-sequence current is small and is reduced to a negligible value by the transposition of conductors. Transposition, however, has no effect on the zero-sequence induction. When paralleling two identical circuits of the positive-sequence network, the resulting impedance is one half of the impedance of one line. This is not true for the zero-sequence network for the reason that the flow of unbalanced currents will cause a certain amount of induction or mutual coupling to exist between the circuits resulting in a value somewhat greater than half the impedance for two identical circuits in parallel. The mutual impedance between the circuits tends to reduce the fault current under certain conditions and increase it under other conditions, de-

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each direction can be independently controlled by connecting different control potentiometers with interlocks on the reversing contactors.

Application to Motor-Generator-Set Drives

For those who have immediate need for this type of control characteristic for motors of, say 50- to 400-horsepower ratings, or even 2,000-horsepower, for that matter, it is practical to modify this equipment so that the thyratron functions associated with variable armature excitation of a one-horsepower motor would, instead, provide field excitation for the generator of a motor generator set, and the thyratron circuit for the field of the one-horsepower motor would be adapted to supply variable field excitation to the field of the larger motor. The loop-current control becomes more complex, particularly since d-c current must be measured directly and its direction of flow ascertained so as to give correct sequence of excitation to the respective fields of generator and motor during acceleration and deceleration. The reversing problem, too, is more complicated because of the necessity of reversing generator field excitation, but all this has been done.

Meanwhile, developments are progressing toward the use of ignitron-type tubes to provide armature excitation for motors of higher horsepower ratings than can now be supplied with the available hot-cathode thyratron tubes.

pending upon the relative directions of the ground current and the induced current. When a single-phase-to-ground fault occurs on an energized line, the resulting zero-sequence currents flowing through all three conductors in the same direction induce a voltage in the coupled circuit tending to cause an induced current to flow in the opposite direction to the one causing it. This may add to or subtract from the existing zero-sequence currents flowing in the coupled circuit to the fault through physical circuit ties.

Under certain conditions the amount of induction may be great enough to reverse the current flow in the coupled circuit when large fault currents flow. It may even change the direction of flow in nearby transformer neutrals, causing the current to flow in reverse of the conventional direction.

Devices for Representing Effect of Mutual Induction

Two lines having mutual induction between them may be represented by an equivalent circuit, provided the lines are bussed at one end. This presupposes that they are operated at the same voltage. The equivalent impedance is determined by subtracting the mutual impedance from the zero-sequence impedance of each line and adding it as a common impedance permitting solution by a reduction of circuits through arithmetical means. See Figure 1A.

This equivalent circuit may be used on an analyzer, but the solution is limited in its scope to lines of the same voltage bussed at one end. A device providing a more general and flexible arrangement is used on the network analyzer with alternating current applied to the analyzer. A transformer with one-to-one ratio is used, shunting an impedance equal to the mutual impedance across one set of terminals such that a voltage drop proportional to the mutual induction is reflected to the coupled circuit, inducing a current in the opposite direction to the originating current. This device may be used for lines of differing voltages and for unbussed lines. The analyzer setup is the same as for the equivalent circuit, and therefore requires that the mutual impedance be subtracted from the zero-sequence impedance of each line and that one side of the transformer be shunted with an impedance equal to the mutual impedance. Either side of the transformer may be shunted by the mutual impedance.

Modern systems may have some rather complicated conditions involving mutual impedance resulting from the construction of several lines on the same right of way, and their operation at different voltages. In solving problems of this type, resort must be made to simplification of circuits because the usual a-c network analyzer has few transformers for representing mutual induction. Where a number of mutual reactances are to be considered, as many simplifications as possible are made to represent the impedances between busses, and expansions are made successively by sections throughout the system as a detailed study is made.

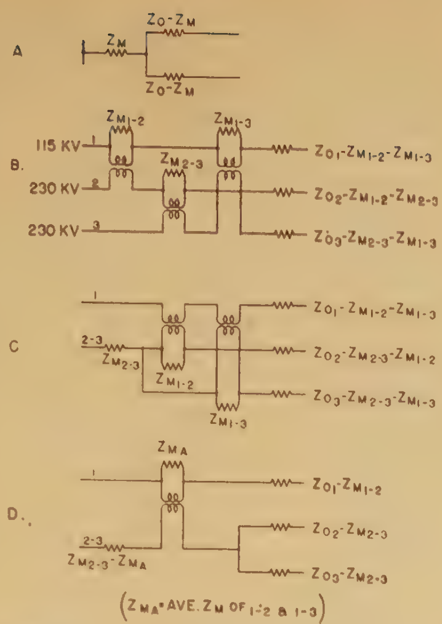

Figure 1A. Equivalent zero-sequence impedance circuit of two parallel lines (same voltage) for calculation of ground-fault current

Figure 1B. Use of one-to-one ratio transformers for representing the mutual induction between parallel lines on the a-c calculating board

Figure 1C. Simplification of the circuits in Figure 1B to reduce the number of one-to-one transformers required for the setup on the calculating board

Figure 1D. Further simplification of Figure 1B. This is only an approximation and is not accurate for all applications

Figure 2 shows a section of a possible system between stations *A*, *B*, and *C* with its several parallels. Figure 3 shows how the section was reduced to an approximate equivalent circuit requiring only two instead of six mutual transformers. Negative values of resistance and reactance may be encountered. The resistance component must be omitted. The reactance component may be handled in one of several ways. Where possible, the negative values of reactance may be combined with the positive values such as would be the case with the primary circuit of the station *B* transformer bank in Figure 3, losing the identity of the station *B* 230-kv bus. Where this cannot be done, a capacitive reactance may be substituted for the negative value of inductive reactance on an a-c analyzer. The diagram in Figure 3 could be further reduced to save reactor units on the analyzer by using a delta-star transformation of the lines, but the net result still shows a negative value of reactance. Negative reactance values can sometimes be avoided if the mutual transformers have a ratio equal to the line voltage ratio. This simplified network would be

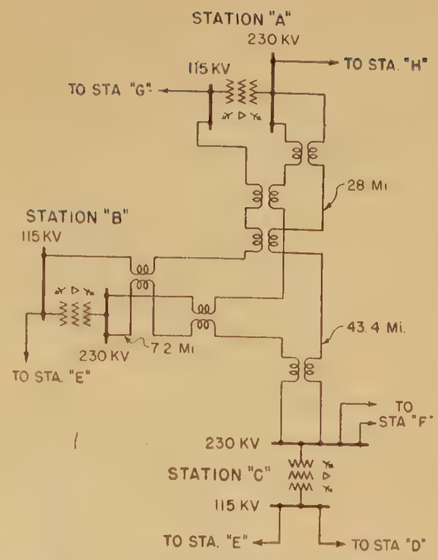

Figure 2. Diagram for indicating the relationships of mutual induction between the parallel lines connecting stations *A*, *B*, and *C*.

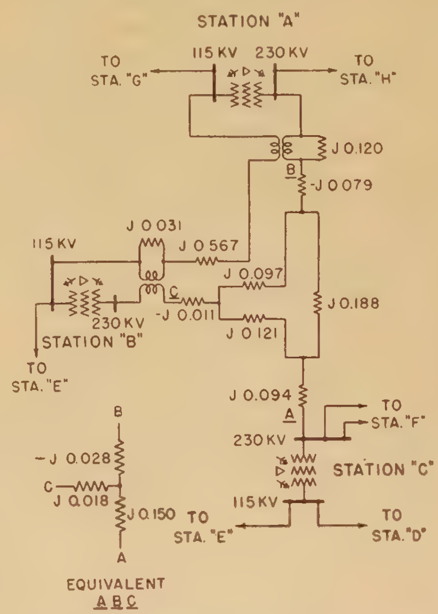

Figure 3. Simplification of the calculating-board circuits representing the lines connecting stations *A*, *B*, and *C* for calculating faults external to this section of the power system

Reactance values are on a per unit 50-megavolt-ampere base

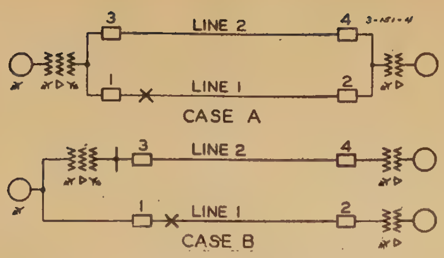

Figure 4. Examples of possible false relay operation

Case A represents two 230-kv lines operated temporarily at 115 kv

Case B represents the same pair of lines, with line 1 operated at 115 kv and line 2 operated at 230 kv, each line energized by a separate generator. The location of the ground fault in each case is marked by an *X*

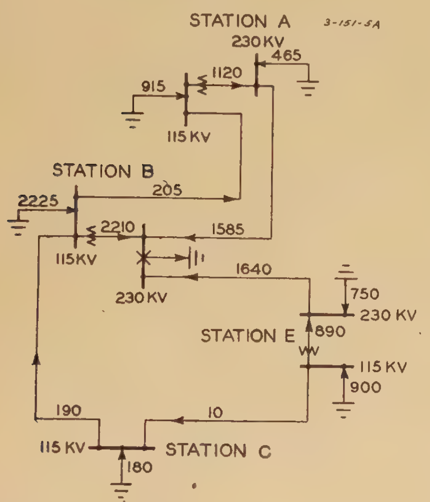

Figure 5A. Direction and magnitudes of the calculated ground-fault currents in amperes at 115 kv for a fault on the 230-kv bus of station *B* with the mutual reactances included for the calculating-board setup

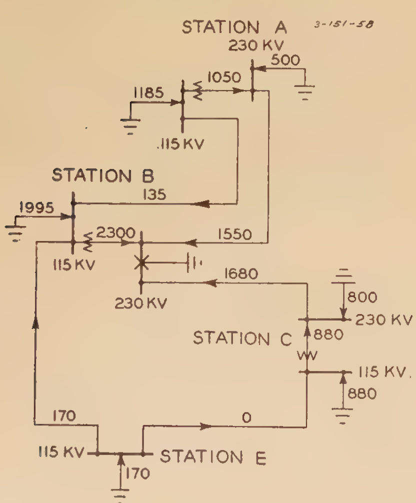

Figure 5B. Direction and magnitudes of the calculated ground-fault currents in amperes at 115 kv if the mutual reactances of the parallel lines are ignored in the calculating-board setup

Compare with current values shown in Figure 5A which include the effects of mutual induction

used during the study of faults in another section of the system and would require expansion for detailed study.

In reducing a network consisting of two lines bussed at one end and paralleled by a third line of different voltage, the arrangement is as shown in Figure 1B.

Figure 1B is reduced as shown in Figure 1C by substituting the equivalent circuit of Figure 1A for the lines that are bussed. When the values of mutual impedance are equal or close enough to average, a single transformer may be substituted as shown in Figure 1D because the sum of the induced currents flow in the mutual branch of the equivalent circuit.

False Relay Operation Because of Mutual Induction

Cases A and B, Figure 4, are two examples of how the effects of mutual induction could cause false relay operation if it is not considered in the relaying.

Case A

Case A represents two 230-kv lines operated temporarily at 115 kv. A power source is connected to each end of the circuit, and the two lines are bussed at both ends.

When the fault occurs as shown near the end of the line, a large current flows through breaker 1 resulting in quick tripping. At the same time the induction-type current-polarized directional ground relay at 2 starts to operate. After breaker 1 opens, the direction of current flow in line 2 reverses, and, because of the extra induced current from mutual induction in the line, the directional overcurrent ground relay in position 3 may seal its contacts before breaker 2 opens, causing an unnecessary trip out of the unfaulted line. Breaker 2 would probably have cleared before relay 3 closed its contacts if the added influence of mutual induction had not increased the current in line 2 to speed up the operation of relay 3.

Case B

Case B, Figure 4, represents the same pair of lines except that circuit 2 has been raised to 230 kv, and each line is energized by separate generators.

The fault is again placed at the same point on line 1, causing breaker 1 to open. This entirely isolates the two lines except for mutual induction between them which caused the unbalanced current to reverse in line 2 and therefore reverse the normal direction of the current flow in the transformer neutral. Relay 4, if current polarized, has both its polarizing and actuating current reversed so the resultant direction continues to be the same until breaker 2 opens. Relay 4 may close its contacts before breaker 2 opens, causing false relay operation of the circuit.

Effect of Mutual Induction on Ground Currents

Figures 5–7, inclusive, show diagrams with data obtained from a system fault study made on the a-c network analyzer and provide examples of the effect of mutual induction between circuits for ground faults. These examples are for the same section of the system as shown in Figure 2 except that the line from station *A* to station *C* is omitted. These

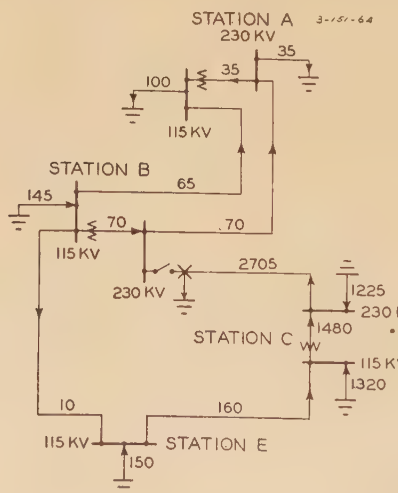

Figure 6A. Because of mutual induction between circuits it is possible to have appreciable ground currents flowing in sections isolated from the ground fault

Note current flow between stations *B* and *A*

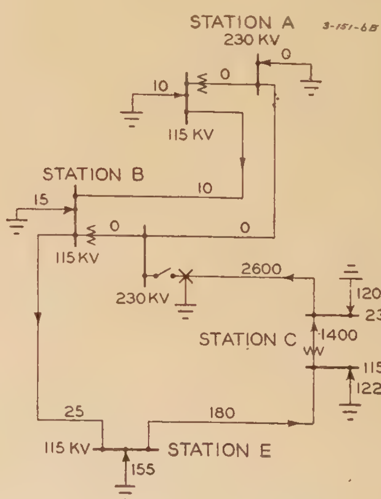

Figure 6B. The effect of ignoring mutual induction

Note negligible flow of ground currents between stations *A* and *B*

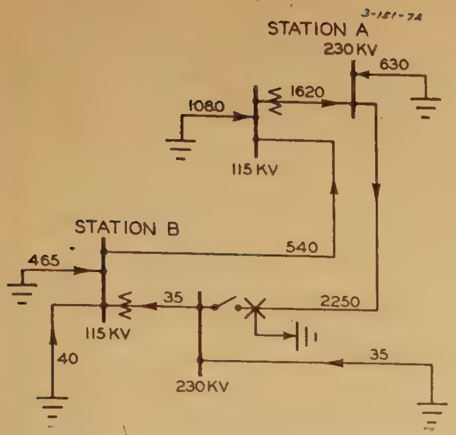

Figure 7A. The effect of mutual induction increases the over-all ground current flowing in parallel lines providing the currents flow in opposite directions

Note the effect on the station *A* to station *B* 115-kv line which parallels the 230-kv line for 35 miles. Compare with Figure 7B

examples show faults on the end of transmission lines with values of ground current flowing before and after the adjacent breaker to the bus has opened for conditions with and without the consideration of mutual induction. All currents shown are equivalent amperes at 115 kv.

Figure 5 shows the effect of mutual induction between the 115-kv and 230-kv lines for a single-phase-to-ground fault on the station *B* 230-kv bus. When mutual induction is considered (Figure 5A), the calculated value of ground current in the 115-kv line is 205 amperes from station *B* to station *A*. However, when the effect of mutual induction is ignored (Figure 5B), the calculations show a flow of only 135 amperes in the 115-kv line, *but in the opposite direction to that shown in Figure 5A*. In this case the 115-kv line might be cleared by relay unnecessarily for a 230-kv line fault if one were not aware of the effect of mutual induction between paralleling lines of different voltages.

It is possible, as shown in Figure 6, to have appreciable ground currents flowing in sections isolated from the fault because of mutual induction between circuits. The effect shown in Figure 6 would be much more pronounced with longer parallels or larger concentrations of ground currents. Figure 6A shows the magnitude and direction of the ground currents circulating between stations *A* and *B* resulting from the effect of mutual induction when a fault occurs on a radial 230-kv line fed from station *C*. If mutual induction is ignored, results will be obtained similar to those shown in Figure 6B. In the latter case practically

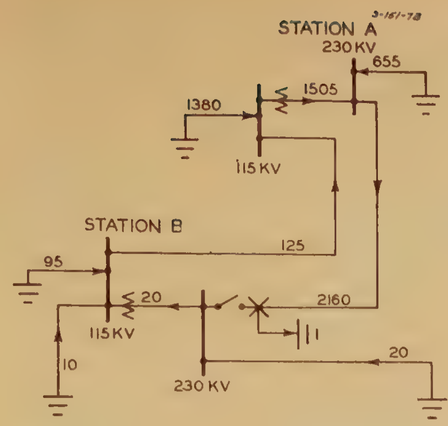

Figure 7B. Mutual induction is ignored in the calculations

The sum of the ground currents circulating in the 115-kv line and the station *B* 115-kv transformer neutral are equal to only 22 per cent of the calculated values of ground currents (shown in Figure 7A) when the mutual induction is included. In this case faulty relay operation could result if the effect of mutual induction is ignored

no current circulates in the lines between stations *A* and *B*.

The effect of mutual induction increases the over-all ground current flowing in parallel lines providing the currents flow in opposite directions. Figure 7 shows this effect very clearly on the station *A* to station *B* 115-kv line which parallels the 230-kv line for 35 miles. The mutual induction (Figure 7A) increases the ground current in the line and the station *B* transformer neutral practically fivefold over the values obtained in Figure 7B, which neglects mutual induction, for the same 230-kv ground fault. This is an instance that could very easily cause false relay operation, since erroneous results would be obtained by neglecting the mutual induction. Proper results could not be obtained on a d-c board since the lines are of different voltages.

Conclusions

A d-c calculating board cannot be used to represent the effects of mutual induction between lines operated at different voltages. The neglect of these effects may give erroneous answers serious enough to result in false relaying.

With two lines paralleling each other on the same right of way through which unbalanced fault current is flowing in the same direction, the effect of mutual induction between the lines is to reduce the flow of ground current.

The effect of mutual induction between two lines paralleling each other on the same right of way through which unbalanced current is flowing in opposite directions is to increase the flow of ground current.

It is possible, because of the effects of mutual induction between lines, to reverse the normal direction of the ground current flowing in the grounded neutral of a transformer bank to a ground fault.

Appendix. Formulas for Calculations of Zero-Sequence Impedances of Short Transmission Link Without Ground Wires

Zero-Sequence Impedance

$$Z_0 = R_c + 0.00477f + j.01397f \times \log_{10}\frac{D_e}{GMR \text{ circuit}}$$

(1) (Reference 1, page 157)

Z_0 = ohms per phase per mile

$$Z_m = 0.00477f + j.01397f \times \log_{10}\frac{D_e}{GMD \text{ circuit}}$$

(1) (Reference 1, page 158)

Z_m = ohms per phase per mile

f = frequency in cycles

D_e = 2,790 feet based on an average ground resistivity of 100 meter-ohms

(1) (Reference 1, pages 146–8)

GMR circuit = $\sqrt[3]{GMD \text{ sep}^2 \times GMR \text{ conductor}}$

(1) (Reference 1, page 157)

GMR conductor obtained from tables

(1) (Reference 1, page 138)

GMD sep = $\sqrt[3]{\text{three distances between conductors}}$

R_c = resistance per mile of one conductor

GMD circuit = $\sqrt[9]{\text{nine distances between conductors}}$

Conversion of ohms to per unit Z

$$\text{Per unit } Z = \frac{\text{base mva} \times \text{ohms}}{\text{kv}^2} \quad (2)$$

For mutual impedance between lines of different voltages

$$\text{Per unit } Z = \frac{\text{base mva}^* \times \text{ohms}}{\text{kv}_1 \times \text{kv}_2}$$

References

1. SYMMETRICAL COMPONENTS (book), C. F. Wagner, R. D. Evans. McGraw-Hill Book Company, Inc., 1933.

2. CALCULATION OF SHORT-CIRCUIT CURRENTS IN A-C NETWORKS, W. M. Hanna. *General Electric Review*, April 1937, pages 189–96.

* Megavolt-amperes.

High-Voltage-Ignition-Cable Design for Aircraft

H. H. WERMINE
NONMEMBER AIEE

IN the design of an insulation, mechanical and chemical problems must be considered as well as the electrical problem. This is particularly true in the design of an ignition cable for aircraft.

Before constructing a suitable insulated wire or cable for any purpose, it is essential to know the conditions of usage. An aircraft ignition cable may be subjected to the following:

1. Abrasion and chafing.
2. Pulling.
3. Compression.
4. Vibration.
5. Heat.
6. Cold.
7. Moisture.
8. Oil and solvents.
9. Ozone.
10. Nitric acid.
11. Low pressures.

In addition, the insulation must have high dielectric strength and low capacitance.

The cable is subjected to abrasion, chafing, and pulling during assembly into the manifold and flexible conduit leads. This is emphasized by the close fitting manifold, made in an effort to save space and weight. Insufficient space is an enemy of insulation. Many engineers often neglect to allow enough space to prevent injury to insulations. It is appreciated that weight and space saving are very important, especially in aircraft, but proper functioning and safety should take precedence over space saving.

Compression of the cable takes place at the tightly fitting grommets within the spark plug and elbow fittings.

Vibration is quite severe while the engine is running.

According to aircraft-engine manufacturers, and engineers of the Air Services, the temperature in the spark-plug elbow may at times reach 325 to 350 degrees Fahrenheit. The temperature at the spark-plug gasket is 400 to 500 degrees Fahrenheit, and the spark-plug barrel or well may reach a temperature of 375 to 400 degrees Fahrenheit.

We are told that temperatures as low as −70 degrees Fahrenheit or even lower are encountered in high altitude flying and reach this at ground level in parts of the world where our planes are in operation. Although the cables are not flexed or moved at these temperatures, spark plugs may be changed at −20 degrees Fahrenheit or even lower. In such event the cable is flexed or bent to some extent.

Although the cable is entirely enclosed in shielding, moisture enters the system because of breathing, and condensation may take place. If moisture is absorbed by any part of the cable, arcing and premature failure may occur.

The cable comes in contact with lubricating oil and with solvents which are used to wash down and clean the engine.

The cable is exposed to the effects of corona. The ozone formed because of the high electrical stress on the air surrounding the cable is a powerful oxidizing agent. The effects of ozone on the cable can be observed internally next to the conductor as well as externally.

Ozone combines with nitrogen of the air to form oxides of nitrogen. These oxides form nitric acid in the presence of moisture. Nitric acid is also a very powerful oxidizing agent.

High-altitude flying has greatly complicated the problem of designing satisfactory aircraft ignition cables. Cables that function reasonably well at moderately low altitudes fail after relatively short service at very high altitudes.

So far as can be learned, the peak voltages impressed on the cables are in the magnitude of from 7.5 to 10.0 kv.

The dielectric constant of the insulation should be such that the capacitance of the cable will be low enough not to interfere with proper firing of the spark plug.

Consideration of the conditions of service makes it clear that designing a cable that will function satisfactorily is no easy problem. The conventional type of rubber-covered, braided, and lacquered cable is basically lacking in certain respects. The lacquer coating is deficient in resistance to abrasion and chafing. It is difficult, if not impossible, to moisture-proof the braid completely. The lacquered braid is readily attacked by the combined action of heat and nitric acid, particularly in the confined space in the spark-plug well and elbow. Furthermore, the lacquer coating when subjected to heat, especially at high altitudes, is seriously affected.

The primary purpose of the lacquer coating is to prevent ozone deterioration of the rubber compound. If the lacquer coating is ruptured, electrical failure is likely to result in a relatively short time.

It is apparent that there is a need for an improved cable, especially for high-altitude flying. The cable designer must therefore find more suitable insulating materials to produce a cable which will meet the requirements. It is also apparent that there must be a meeting of minds of the cable manufacturers, the harness manufacturers, the aircraft-engine manufacturers, and the engineers of the Air Services on certain compromises. It is unlikely that a cable can be made that will meet all the conditions enumerated previously to the extent desired by each individual engineer. One engineer may emphasize heat resistance, another cold resistance, or some other requirement. This emphasis on any specific property can only be met at the expense of some other equally important requirement. What we must arrive at is a balance of properties so that we will obtain a cable with the maximum of any one characteristic without undue reduction of any other necessary characteristics.

The present trend in specification writing is to emphasize performance tests rather than constructional details or requirements based on specific materials. Very often specifications which are not based on performance will prevent the development of improved products. This can occur when new materials become available which will meet conditions of service more satisfactorily but may fail to meet tests based on specific materials. Performance specifications on the other hand encourage development, use of new materials, and better design. It is more difficult to prepare performance specifications since the object is to simulate conditions of service with a reasonable margin of safety. However, when such specifications are finally achieved, it will usually procure a product more satisfactory for the intended purpose.

It is unusually difficult to determine the suitability of an aircraft-engine igni-

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H. H. Wermine is chief engineer for Belden Manufacturing Company, Chicago, Ill.

tion cable. It is at best a long procedure to make power-plant tests, and heretofore laboratory tests have revealed comparatively little as to how the cable would perform in actual flight.

Ignition-cable manufacturers have had this under consideration for some time and have attempted to prepare performance specifications that would reproduce as closely as possible conditions actually encountered in service. One test method suggested for incorporation in a specification is termed "simulated flight test." Figure 1 shows this apparatus. It consists principally of a bell jar mounted on a base plate, with proper fittings for attachment of a spark-plug chamber, a spark plug and elbow assembly with a length of standard flexible conduit. A suitable heater coil is mounted around the elbow and spark plug. The bell jar is partially evacuated to the desired pressure. The sample of cable to be tested is assembled in the flexible conduit lead, and the assembly at the spark plug is made identical with that made on an actual engine installation.

These test conditions have been suggested and may be modified as more experience and data are obtained in testing with the apparatus:

Gasket temperature	500 ± 10 degrees Fahrenheit
Elbow temperature	325 ± 5 degrees Fahrenheit
Pressure in electrode chamber	20 ± 5 pounds
Absolute pressure in vacuum chamber	10 ± 0.5 centimeters mercury
Peak voltage	12.5 ± 0.5 kv
Added capacitance	250 micromicrofarad

An attempt has been made to simulate flight conditions by cycling as shown in the tabulation:

Time	Heat	Vacuum	Voltage
Start	On	On	On
1/4 hour		Off	
1 1/2 hours		On	
2 hours	Off		
2 1/4 hours		Off	
3 hours	On	On	On

The voltage is on continuously, and the schedule is continued a definite number of cycles or until failure occurs.

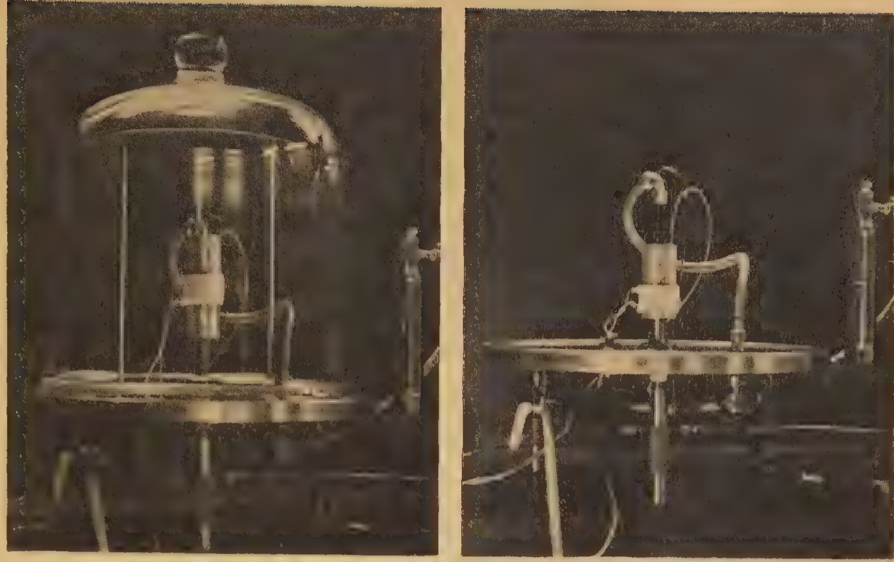


Figure 1. "Simulated flight test" apparatus with and without bell jar

The cycling is shown graphically in Figure 2.

This test method has been in use by cable manufacturers, and a great deal has been learned about faults and deficiencies of present types of cables. It has also been very useful in the testing of new designs.

Aircraft-Ignition-Cable Design

Conductor

The size of the conductor materially affects the capacitance of the cable, and its cross-sectional area should therefore be as small as possible, consistent with sufficient tensile strength for manufacturing and normal handling. The conductor commonly used for aircraft ignition cable is made with seven strands of 0.013-inch diameter corrosion resisting steel.

The conductor should be smooth, and the stranding should be concentric. This type of stranding most nearly approaches the form of a solid conductor. Any accentuated unevenness or roughness tends to concentrate the electric stress and promote puncture.

It has been found advantageous to use a "sealed" conductor in the construction of aircraft ignition cables to prevent air leakage through the conductor. Leakage occurs at high-altitude flying when the pressure in the spark-plug well is greater than that of the surrounding atmosphere. In case a sealing compound is used in the spark-plug well, it is apt to be forced into the cable along and through the interstices of the cabled conductor. The presence of air inside the cable will also result in the formation of ozone which may lead to internal failure.

The sealing of the conductor results in better adhesion between the conductor and the insulation, thus preventing the insulation from sliding on the conductor. A sealed conductor tends to reduce twisting or dislocation of the conductor which may be the cause of internal corona cracking because displacement of the conductor with respect to the insulation will permit air to enter the cable along the conductor. Twisting or dislocation places the insulation under stress. Stressed insulation is readily attacked by ozone, resulting in cracking and ultimate failure.

The sealing of the conductor is accomplished by extruding or coating the strand of wire used in the center of the cabled conductor with a suitable compound. When the other six strands are cabled around the coated wire, the compound is forced between the individual strands, completely sealing the conductor

Insulation

If a sealed conductor is used, a properly designed rubber compound is satisfactory, since the effect of internal corona is eliminated. Rubber compounds have advantages in that the low temperature flexibility is excellent and electrical properties are good. The rubber stock pile is being rapidly reduced, and it may become necessary to find a substitute. Some of

Table I. Insulations

Material	Dielectric Strength	Resistance to Compression	Heat Aging	Ozone Resistance	Low Temperature Flexibility	Capacitance
Rubber	1	1	2	4	1	1
Buna *S*	1	1	1	4	2	1
Butyl rubber	1	2	1	2	3	1
Polyisobutylene	1	2	1	3	3	1

the synthetic rubbers appear to be suitable, and among those are the buna *S* and isobutylene types. Cables have been made with these substitutes, and tests indicate that they are quite satisfactory.

In Table I are listed materials which have been used or which are under investigation. It is assumed that the material is properly compounded for the application. The materials are rated in accordance with their comparative value as insulation for ignition cable. The lowest number indicates the greatest value.

Protective Covering

A lacquer coating applied over a braid is used to prevent deterioration of the insulation by ozone, oils, and solvents. It is lacking in resistance to abrasion and chafing. The lacquered braid does not have sufficient resistance to heat and nitric acid. It is, therefore, necessary to use a more suitable protective covering.

is also highest at this location. Heat combined with low pressure tend to cause blistering or swelling of the protective covering. With lacquered cable, blistering might occur on the first flight at very high altitudes. The condition can be readily reproduced in the "simulated flight test."

The disintegrated lacquer seems to contaminate the porcelain or other type of sleeving placed over the end of the cable as well as the insulating wall of the spark plug. This contamination may induce arc-over from the spring contact to the outside casing of the spark plug. The arc-over is most likely to occur at high altitudes, particularly if the Neoprene grommet does not completely seal the spark-plug well.

The most satisfactory material for a protective covering at present appears to be suitably compounded Neoprene. Neoprene sheaths are reasonably ozone resistant and are improved by incorporating into the compound antioxidants and waxy materials that tend to migrate to the surface. A coating of wax applied to the cable will also improve corona resistance.

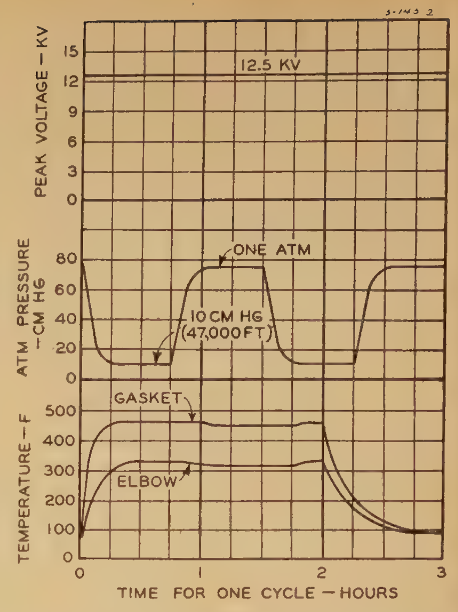


Figure 2. Graphs showing heat, atmospheric pressure, and voltage conditions during "simulated flight test"

Table II. Protective Coverings

Material	Resistance	Moisture Resistance	Heat Resistance	Ozone Resistance	Abrasion and Chafing Resistance	Low Temperature Flexibility
Buna *N'S*	2	1	1	3	1	1
Neoprene	2	1	1	2	1	1
Thiokol	1	1	3	1	1	1

In Table II are shown some of the materials that have been investigated and applied as protective coverings for ignition cables. It is assumed that the material is properly compounded for the application. The materials are rated in accordance with their comparative value as protective covering. The lowest number indicates the greatest value.

Most of the failures of ignition cable on aircraft engines occur in the spark-plug well or spark-plug elbow. This is readily understandable, since the cable is subjected to intense ozone concentration at this point as well as nitric acid formed by the combination of ozone, nitrogen, and moisture. The temperature

Neoprene-sheathed cables have good low temperature flexibility and will withstand flexing at −40 degrees Fahrenheit. These cables should not be bent or flexed at lower temperatures. This resistance to low temperature is better than that of lacquered cables which should not be flexed at temperatures lower than −20 degrees Fahrenheit.

As previously stated, heat and low pressure existing at high altitudes tend to cause blistering or swelling of the Neoprene sheath at the spark-plug elbow. This exposes the insulation to the conditions in the spark-plug well and elbow.

It has been found that one factor causing blistering of Neoprene-sheathed cable is that the moisture absorbed by the cable in the steam vulcanization process tends to expand because of the temperature at the elbow. At low pressures the tendency to blister is greatly increased. The blistering caused by moisture can be prevented by preheating the Neoprene-sheathed cable for 24 hours at 180 degrees Fahrenheit.

Because of the limited space in the ignition-cable manifold and the increased surface friction of Neoprene-sheathed cables, there is greater mechanical abuse in wiring the harness than when lacquered cable is used. A glass-yarn braid applied between the primary insulation and the Neoprene sheath will impart added mechanical strength to the cable and help prevent stretching and twisting of the cable during manifold assembly. A glass-yarn braid also materially increases ozone resistance of the cable.

One disadvantage of a glass-yarn braid is the relative difficulty of sealing the cable when the pressure surrounding the spark plug and elbow is low as occurs at high altitudes. Air tends to flow through the interstices of the braid which may cause blistering or swelling of the Neoprene sheath at the elbow and lower the pressure in the spark-plug well. The spark-plug-well sealing compound may be forced up into the braid. This tendency of leakage through the braid can be partially overcome by applying a thin sheath of Neoprene over the insulation.

Table III. Relative Merit of Cables

	Braided With Lacquered Coating	Neoprene Sheath No Braid	Neoprene Sheath Glass-Yarn Braid Over Insulation	Neoprene Sheath Glass-Yarn Braid Between Layers of Neoprene
Abrasion and chafing	Poor	Excellent	Excellent	Excellent
Pulling	Fair	Poor	Excellent	Excellent
Compression	Poor	Fair	Fair	Fair
Heat	Fair	Good	Good	Good
Cold	Fair	Good	Good	Good
Moisture	Fair	Excellent	Good	Good
Oil	Excellent	Good	Good	Good
Aromatic solvents	Poor	Fair	Fair	Good
Ozone	Excellent	Good	Good	Excellent
Nitric acid	Poor	Excellent	Excellent	Excellent
Low pressure	Poor	Excellent	Fair	Good

Figure 3. Four ignition-cable constructions

A. Sealed conductor; insulation; Neoprene sheath

B. Sealed conductor; insulation; glass-yarn braid; Neoprene sheath

C. Sealed conductor; insulation; Neoprene layer; glass-yarn braid; Neoprene sheath

D. Conductor; insulation; cotton yarn braid; lacquer coating

The glass-yarn braid is then applied, followed by a second sheath of Neoprene. The braid thus becomes thoroughly imbedded between the layers of Neoprene. This construction further increases the ozone resistance of the cable.

Figure 3 shows constructions which have been discussed. Figure 4 shows deterioration of lacquered-type cable at spark-plug elbow after being subjected to "simulated flight test" and after service on aircraft engine.

Use of Five-Millimeter Cable in Place of Seven-Millimeter Cable

In the wiring of the conventional type of manifold, the seven-millimeter cable receives considerable abuse. Undue pulling or twisting of the ignition cable in harness wiring may occur, resulting in very serious permanent injury to the insulation. Twisting of the cable is apt to produce air pockets along the conductor because of dislocation of the insulation with respect to the conductor. Excessive pulling on the cable may result

Figure 4. Samples of lacquered cable showing deterioration at spark-plug elbow

A. After "simulated flight test"

B. After service on aircraft engine

in elongation of the conductor and stressing of the insulation. The Neoprene-sheathed cable is more resistant to abrasion and chafing than braided and lacquered type, but nevertheless must be handled with reasonable care. Because of the limitations of space, it is doubtful whether seven-millimeter Neoprene-sheathed cable can be assembled in the present manifolds without injury to the cable. The use of five-millimeter cable in place of seven-millimeter cable would simplify the problems of harness assembly and remove the principal source of cable abuse.

Since the puncturing voltage of the five-millimeter Neoprene-sheathed cable is ample, and since cable failure is largely due to deficiencies in chemical and physical properties, it appears that this cable would be entirely satisfactory. The capacitance of an installation with five-millimeter cable is satisfactory since with present manifold and flexible conduit sizes and the same type of cable, the capacitance of the individual leads is less than when seven-millimeter cable is used because of the additional air space in the manifold. This favorable condition would only exist so long as the manifold size remains the same or is reduced only to a point that ample space is allowed to prevent mechanical injury to the insulation of the five-millimeter cable. Should the manifold size be reduced to fit the five-millimeter cable as closely as the present manifold fits the seven-millimeter cable, the same objectionable difficulties in assembly will exist.

Four types of cables have been under consideration. None of these cables meet all of the requirements to the extent desired. Any one is superior in some respects and deficient in other respects. It must be recognized that some of the requirements asked for by aircraft-engine manufacturers and engineers of the Air Services cannot be fully met. As an example, if it is essential to have a cable withstand the solvent action of highly aromatic gasolines, none of the cables under consideration would be entirely satisfactory. Again, if flexibility much below −40 degrees Fahrenheit is required, these cables would not be suitable.

In Table III, an attempt has been made to estimate the relative merits of the four cable constructions under consideration as to the conditions of usage on engine installations previously outlined.

Cable manufacturers at present are doing intensive development work in an effort to improve aircraft ignition cables. The numerous new materials which are being made available by the chemical industry are being thoroughly investigated, and advantage is being taken of those showing promise. Recent close cooperation between the engineers of the Air Services, the aircraft-engine manufacturers, and the cable manufacturers has greatly accelerated the development and improvement of ignition cables, and this will no doubt result in continued progress.

Dethermalizing Arc Quenchers

LLOYD F. HUNT
FELLOW AIEE

FRED H. COLE
MEMBER AIEE

Synopsis: The primary function of the normal-type oil circuit breaker is to interrupt the circuit as rapidly as possible, without disturbing the connected system, and to leave the oil circuit breaker in substantially the same electrical and mechanical condition as before. The interruption of an electrical circuit is always accompanied by an arc, and it has been found that if the arc and its associated space are rapidly cooled or dethermalized it is readily extinguished. This paper describes dethermalizing arc quenchers that cause the arc and its associated space to be cooled by the controlled flow of oil through and around the arc. The action of these quenchers has been verified by field tests on the 16-kv and 220-kv systems of The Southern California Edison Company Ltd.

IN the operation of large electric systems, effort is continuously exerted to gain reliability and at the same time to cut down maintenance costs. One of the costly maintenance problems is the overhauling of oil circuit breakers. It is the customary rule, based on experience, to overhaul oil circuit breakers after each short circuit or at least after two interruptions of short circuit. The overhaul periods of the oil circuit breakers could be greatly lengthened if the contact burning and the oil carbonization could be reduced. In order to accomplish these desired results it is important to cut down arcing time and contact separation during the arcing period.

Conclusion

The proper design of dethermalizing arc quenchers will interrupt short circuits of any magnitude without carbonizing the oil or changing its dielectric value, and because of the short arcing time the burning of the contacts becomes negligible.

Even though this short arcing time is accompanied by an extremely short contact separation, the continuously flowing oil prevents restriking of the arc. Consequently, with short arcing time and short arc lengths the arc energy is so greatly reduced that mechanical disturbances are eliminated.

The field tests on the Southern California Edison Company Ltd. system proved that the dethermalizers are adequate to interrupt short circuits ranging from a minimum to maximum duty without mechanical disturbance to the switch and without electrical disturbance to the system. The time of interruption decreased with the greater duty, thus giving the switch a larger factor of safety or an increased kilovolt-ampere rating.

Tests were made on the Edison system at widely separated locations, resulting in the same interrupting characteristics; that is, with the same kilovolt-ampere duty, the arcing time and contact separation were substantially the same, proving that the dethermalizers are adequate for all locations on the Edison system.

The record of operation of these dethermalizers during the past six or seven years has shown no deterioration of oil contacts, or insulating materials of the interrupters, and they have cleared all short circuits and line dropping incident to normal operation.

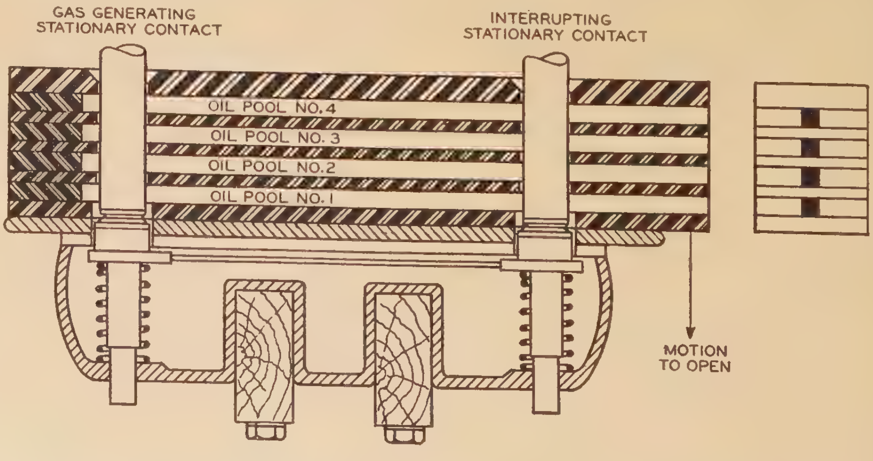
From this operating experience it becomes apparent that this type of arc quencher will result in the very minimum of maintenance and at the same time will give a maximum of system protection.

16-Kv Field Tests, August 25, 1936

Since the Edison company is practically the only company that uses 16 kv for distribution, it was desirable to have a breaker for this voltage with approximately 50,000-kva interrupting rating, small in size and fast in operation. Breakers of the normal type had insufficient interrupting capacity and required considerable maintenance. The dethermalizer for this breaker is shown in Figure 1. It can be seen from this sketch that the dethermalizing element has a multiplicity of pools of oil that are connected to restricted ports on one end, the other end is closed. The arc at the closed end generates gas which produces sufficient pressure to drive the oil in the pool, located between the contacts, through the arc and associated space of the other contact. As the contacts separate, other pools of oil are successively driven into the arc and the arc space of the second contact thus continually cooling the arc and the arc space until a point of extinction occurs, and sufficient cool oil is maintained through this space even after extinction so that restriking does not occur. This rapid cooling of the arc and its associated space is the dethermalizing action that extinguishes the arc.

Figure 2 illustrates a 16-kv 50,000-kva series-trip mechanical trip-free breaker set up for test. This breaker interrupted a 53,000-kva three-phase short circuit on a delta system feeding only the setup from a separate transformer bank. It operated on a close–open test in $3^1/_2$ cycles total time. Figure 3 shows the oscillogram of this test. This switch was closed by hand on a three-phase short circuit and cleared with no disturbance. This test was repeated several times with the same results and when examined showed very little deterioration of contacts, oil, and dethermalizer. Breakers of this design have an operating record of six and one-half years, show no signs of oil carbonization, and have not required any maintenance.

Figure 1. 16-kv dethermalizer, flat-plate construction



Paper 43-142, recommended by the AIEE committee on protective devices for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943. Manuscript submitted June 18, 1943; made available for printing July 20, 1943.

LLOYD F. HUNT is senior electrical engineer, Southern California Edison Company Ltd., Los Angeles, Calif.

FRED H. COLE is president, Cole Electric Company, and consulting engineer, Kelman Electric and Manufacturing Company, Los Angeles, Calif.

The authors wish to acknowledge the assistance of their associates in the Southern California Edison Company Ltd. and Kelman Electric and Manufacturing Company.

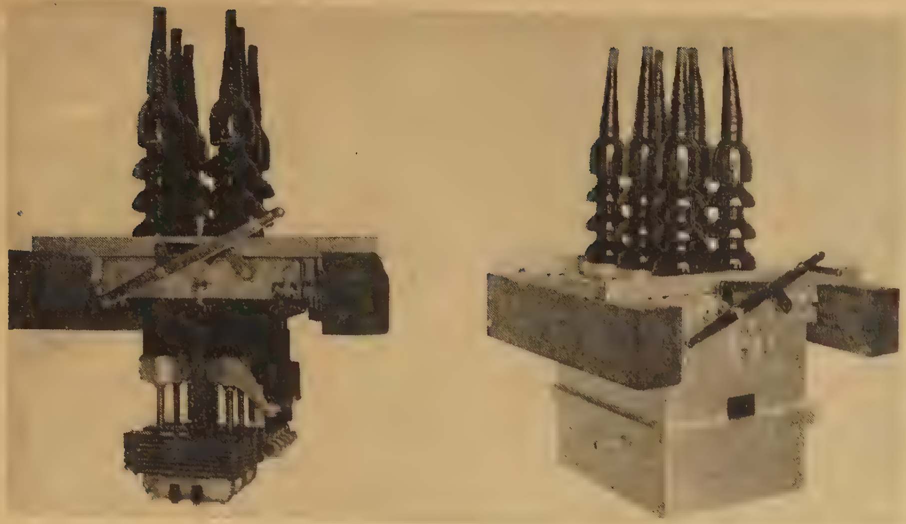


Figure 2. 16-kv oil circuit breaker with dethermalizers set up for field test

Figure 3. 16-kv close–open test, 53,000-kva three-phase short circuit

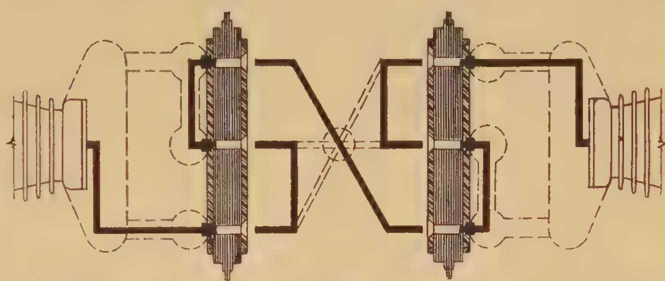


Figure 4. Arrangement of six-break dethermalizers

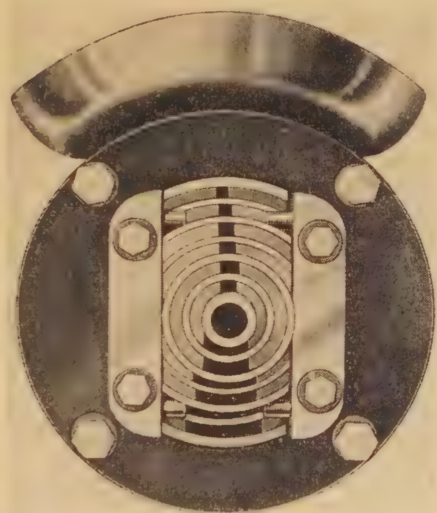


Figure 5. End view of 230-kv tube-type dethermalizer

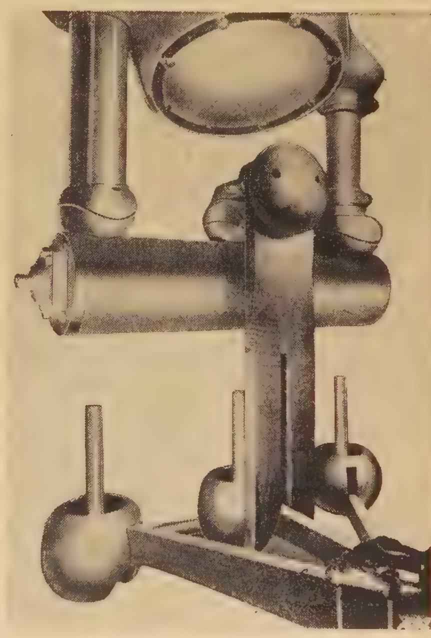


Figure 6 (right). Assembly of 230-kv dethermalizer on switch bushing

230-Kv Dethermalizers

The dethermalizers for the 230-kv oil circuit breakers are made in the form of six breaks per pole or three breaks per bushing. The electric connections and sequence of these breaks are shown by Figure 4. The dethermalizers are made in the form of large tubes. Figure 5 shows the end view of the tube type. The graded ports can be seen in this figure. Each dethermalizer is mounted on the end of the switch bushing as shown by the assembly photograph, Figure 6. This illustration clearly shows the shielding and one-half the crosshead.

Figure 7 shows the dethermalizer of the flat-type construction and the various oil pools or reservoirs. The end view shows the graded ports and the plan view shows the restriction of the ports.

When the switch contacts are parting, arcs are formed at each contact. The arc at the center contact will generate sufficient pressure to force the oil in the pools (located between the center contact and the outside contacts) through the outside arcs and through the ports at each end of the large tube. As the contacts are opening, new pools of oil are successively forced through the outside

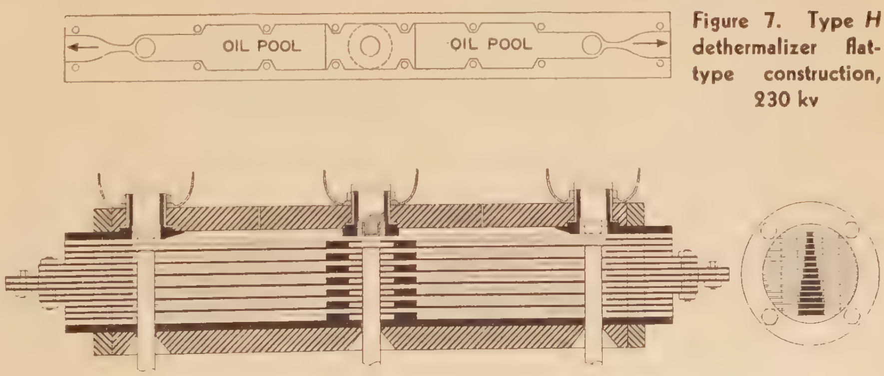


Figure 7. Type *H* dethermalizer flat-type construction, 230 kv

Table I. Tests Made at Saugus Substation, October 4, 1936

Kelman Dethermalizing Arc Quenchers, Type As Indicated

Test Number	Initial Voltage Phase to Phase (Kilovolts)	Type of Quencher Tube	Number of Breaks Per Phase	Phase Tank	Duty Cycle	Current Interrupted (Rms Amperes)	Equivalent Three-Phase Kilovolt-Amperes Interrupted	Closing Time (Cycles)	Over-all Time of Short Circuit (Cycles)	Relay Time (Cycles)	Breaker Interrupting Time (Cycles)	Contact Separation When Arc Extinguished (Inches)	Total Arc Length (Inches)
1	220	*C*	6	*A*	*O*	778	296,470		8.0	1.0	7.0	3 1/8	18 3/4
1*A*	220	*C*	6	*A*	*CO*	883	336,480	65	7.0	1.0	6.0	3	18
1*C*	220	*	6	*C*	*O*	787	299,897		6.5	1.0	5.5	2 1/4	13 1/2
1*AC*	220	*	6	*C*	*CO*	777	296,090	67	9.0	1.0	8.0	3 1/2	21
2	220	*C*	6	*B*	*O*	1,162	442,800		6.5	0.75	5.75	1 3/8	8 1/4
2*A*	220	*C*	6	*B*	*CO*	1,171	446,225	68	6.5	1.0	5.5	1 3/8	8 1/4
3	220	*C*	6	*B*	*O*	Not recorded because of oscillograph film choke. Same kilovolt amperes interrupted as test 3A							
3*A*	220	*C*	6	*B*	*CO*	2,055	783,090	69	7.0	0.75	6.25	1 5/8	9 3/4
4	220	*C*	6	*B*	*O*	4,142	1,578,363		5.5	0.75	4.75	1 1/8	6 3/4
4*A*	220	*C*	6	*B*	*CO*	4,526	1,730,000	69	5.5	1.0	4.5	1 1/8	6 3/4
4*A*1	220	*C*	6	*B*	*CO*	4,249	1,619,140	69	5.3	1.0	4.3	1	6

* In tests 1*C* and 1*AC* the dethermalizers were of the flat-plate construction, and the ports were not graded. All other tests were made with the type *C* dethermalizers which were of the round-tube construction with graded ports.

O—Open. *CO*—Close-open.

Table II. Tests Made at Laguna Bell Substation

Tests Made on July 10, 1938, on Kelman RA6J Breaker, 1,200 Amperes, 230 Kv, Serial Number 16408

Test Number	Initial Voltage Phase to Phase (Kilovolts)	Type of Quencher Tube	Number of Breaks Per Phase	Tank	Duty Cycle	Current Interrupted (Rms Amperes)	Equivalent Three-Phase Kilovolt-Amperes Interrupted	Over-all Time of Short Circuit (Cycles)	Relay Time (Cycles)	Breaker Interrupting Time (Cycles)	Breaker Dead Time (Cycles)	Arcing Time (Cycles)	Contact Separation When Arc Extinguished (Inches)	Total Arc Length (Inches)
Test Made on July 10, 1938, Original Installation, 1 1/4-Inch Bayonets and 1 1/2-Inch Clearance Holes														
1	210	*A*	6	West	*O*	660	240,000	25.0	0.36	24.64	2.60	22.04	17 3/8	104 1/4
1*A*	210	*A*	6	West	*CO*	630	230,000	12.2	0.48	11.72	3.94	7.78	5 1/4	31 1/2
1_1	210	*A*	6	West	*O*	660	240,000	12.0	0.39	11.61	2.80	8.81	5 1/2	33
1_1A	210	*A*	6	West	*CO*	660†	240,000	24.5	0.43	24.07	3.90	20 17	17 1/2	105
Note: Larger values of short-circuit current were not imposed because of the excessive arc length.														
Test Made on July 31, 1938														
1-*C*	210	*C*	6	East	*O*		No record obtained							
1-*C*	210	*C*	6	East	*O*	576	210,000	8.10	0.37	7.73	3.26	4.47	2 3/8	14 1/4
1-*G*	210	*G*	6	West	*O*	576	210,000	8.02	0.40	7.62	2.75	4.87	2 5/8	15 3/4
1-*H*	210	*H*	6	Middle	*O*	573	209,000	7.97	0.36	7.61	2.71	4.90	2 3/4	16 1/2
1*A*-*C*	210	*C*	6	East	*CO*	570	207,000	10.3	0.34	9.95	4.11	5.85	3 5/8	21 3/4
1*A*-*G*	210	*G*	6	West	*CO*	576	210,000	10.47	0.33	10.14	4.03	6.11	3 7/8	23 1/4
1*A*-*H*	210	*H*	6	Middle	*CO*	576	210,000	8.92	0.31	8.61	4.21	4.4	2 5/8	15 3/4
2-*C*	210	*C*	6	East	*O*	1,517	551,000	6.54	0.26	6.28	2.79	3.49	1 3/4	10 1/2
2-*G*	210	*G*	6	West	*O*	1,529	557,000	6.55	0.37	6.18	3.09	3.09	1 1/2	9
2-*H*	210	*H*	6	Middle	*O*	1,520	553,000	6.02	0.30	5.72	2.72	3.00	1 3/8	8 1/
2*A*-*C*	210	*C*	6	East	*CO*	1,529	556,000	7.09	0.29	6.80	4.08	2.72	1 5/8	9 3/4
2*A*-*G*	210	*G*	6	West	*CO*	1,525	555,000	8.00	0.34	7.66	4.14	3.52	2	12
2*A*-*H*	210	*H*	6	Middle	*CO*	1,517	552,000	7.68	0.36	7.32	4.06	3.26	1 7/8	11 1/4
3-*C*	210	*C*	6	East	*O*	2,720	990,000	6.55	0.31	6.24	2.98	3.26	1 5/8	9 3/4
3-*G*	210	*G*	6	West	*O*	2,780	1,011,000	6.05	0.28	5.77	3.01	2.76	1 [illegible]/8	8 1/4
3-*H*	210	*H*	6	Middle	*O*	2,750	1,000,000	5.04	0.46	4.58	2.79	1.79	3/4	4 1/2
3*A*-*C*	210	*C*	6	East	*CO*	2,659	967,000	6.59	0.40	6.19	4.17	2.02	1 1/4	7 1/2
3*A*-*G*	210	*G*	6	West	*CO*	2,639	960,000	7.20	0.38	6.82	4.02	2.80	1 3/4	10 1/2
3*A*-*H*	210	*H*	6	Middle	*CO*	2,610	950,000	6.63	0.30	6.33	4.03	2.30	1 3/8	8 1/4
4-*G*	210	*G*	6	West	*O*		No record obtained							
4*A*-*G*	210	*G*	6	West	*CO*	4,720	1,719,000	6.06	0.35	5.71	3.89	1.82	1	6
4*A*-*H*	210	*H*	6	Middle	*CO*	5,075	1,850,000	5.97	0.30	5.67	4.05	1.62	1	6
5-*H**	210	*H*	6	Middle	*O*	6,200	2,259,000	5.48	0.25	5.23	3.26	1.97	3/4	4 1/2
5*A*-*H**	210	*H*	6	Middle	*CO*	6,070	2,100,000	6.06	0.30	5.76	4.20	1.56	7/8	5 1/4

* See oscillograms in Figures 11 and 12.

Types of quencher tubes: Type *C*—Round-tube type as tested at Saugus 10-4-36. 1.050-inch bayonets, center clearance-hole 1 3/16 inch and clearance-holes round-end, 1 3/16 inch x 1 7/16 inch.

Type *G*—Flat-plate style, designed to have the characteristics of type *C*, as nearly as practical. 1-inch bayonets, clearance-holes 1 1/16 inches.

Type *H*—Flat-plate style. 1-inch bayonets, clearance-holes 1 1/16 inches. Graded ports.

After the test the contacts were found to be slightly burned, and a slight carbon deposit was found on the quencher tubes. A test of the oil showed no visible trace of carbon.

O—Open. *CO*—Close-open.

† Larger values of short-circuit current were not imposed because of excessive arc lengths.

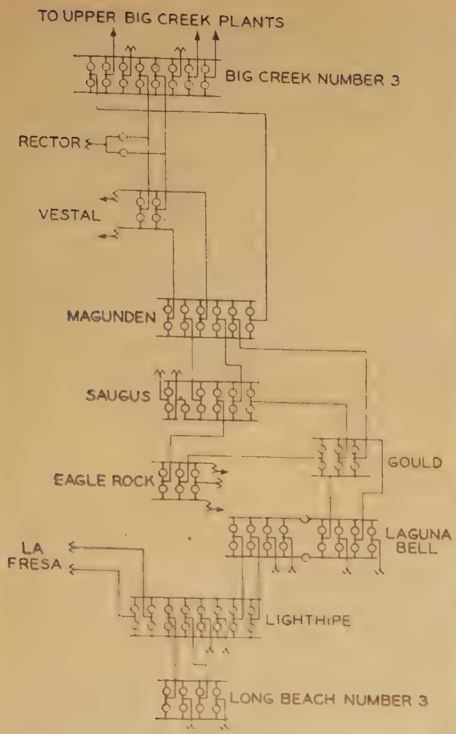


Figure 8. 220-kv system of the Southern California Edison Company, Ltd.

arcs until the circuit is interrupted. At the time of extinction, fresh oil is between the contacts so no restriking occurs. In this arc quencher fourteen ports are used. The tube type operates in the same manner.

Further development has been made on a two-break device which will be the subject of a future paper.

Saugus and Laguna Bell Substations—Field Tests

In order to check the operation of these dethermalizers, actual phase-to-ground short-circuit tests were made on the main 220-kv system at the Saugus and Laguna Bell substations of the Southern California Edison Company Ltd. Figure 8 shows the 220-kv connections of the Edison system at the time of these tests.

The various kilovolt-ampere duties for the switch under tests were obtained by system arrangements. The lowest duty at these two locations was accomplished by feeding 220 kv to 66 kv and then back to 220 kv. This gave the impedance of the two transformer banks in series for limiting the short-circuit kilovolt-amperes. The other steps were accomplished by separating a line from a distant station, which supplied the test breaker. Then by selecting a shorter line the kilovolt-amperes duty was increased, and the final test at the two stations was the maximum kilovolt-amperes that could be delivered at the 220-kv bus, that is, all lines and transformer banks in service. At the Saugus substation this amounted to an equivalent three-phase short circuit of 1,730,000 kva, and at Laguna Bell it amounted to 2,259,000 kva.

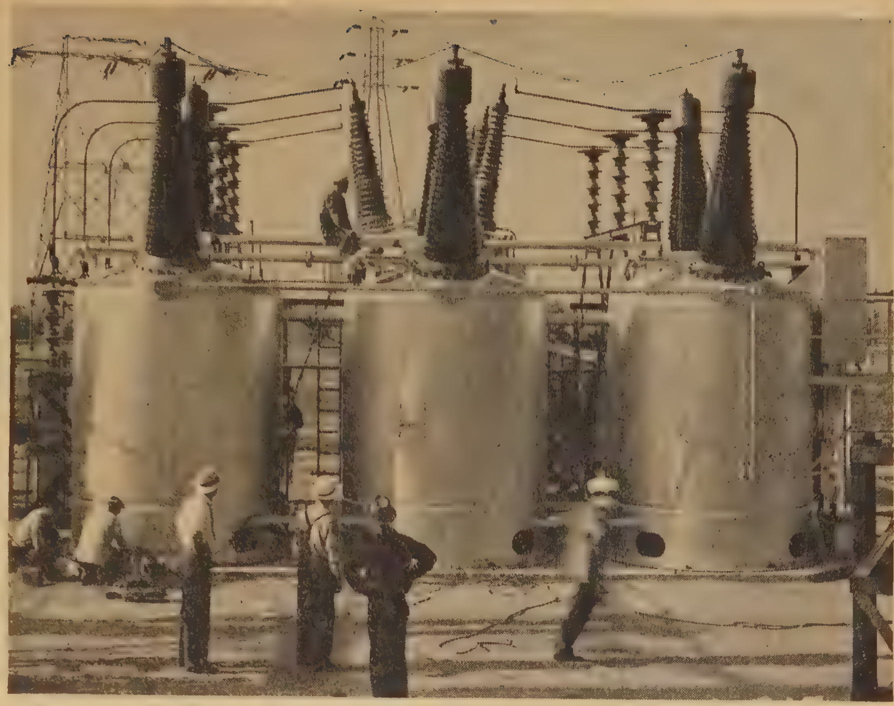


Figure 9. Kelman 230-kv 1,200-ampere 2,500,000-kva oil circuit breaker equipped with dethermalizer

The breakers at both locations performed perfectly throughout the tests without any mechanical trouble or without disturbing the system. After the series of tests the oil was tested and filtered and did not show any perceptible increase of carbon in the oil.

Figure 9 shows the test breaker at Laguna Bell substation. Figure 10 illustrates bars of various lengths that were placed on end on a steel plate between two tanks of the breaker. The mechanical movement of the breaker under test was so small that none of these bars were knocked over.

Table I is the tabulation of the tests taken at Saugus substation on October 4, 1936.

Table II is the tabulation of the tests taken at Laguna Bell substation on July 10, 1938 and July 31, 1938.

In these tests the type *C* dethermalizers are of the tube type, as shown by Figure 5, the *H* type are as shown by Figure 7, and the *A* type are of the flat-plate construction with more restricted ports.

The series of tests on July 10, 1938 were not continued above the values

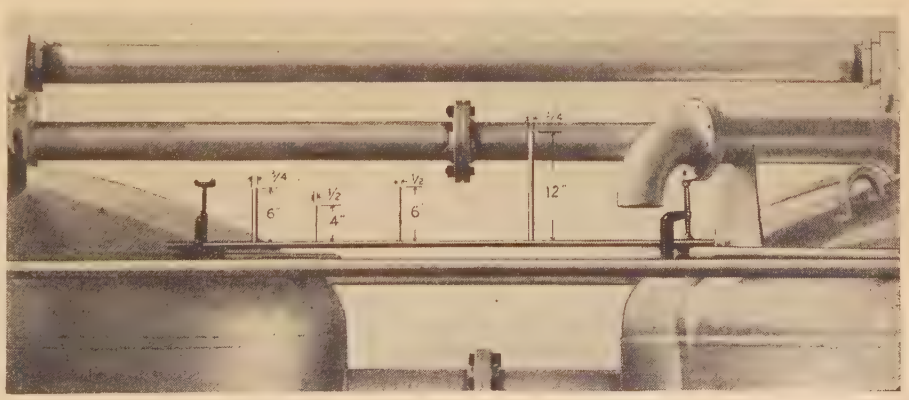


Figure 10. Rods for determination of switch movement

The four steel pins were set on end to test for any jar to the breaker during opening of short circuits. Pins were turned square on ends and set on a 1-inch by 4-inch steel bar. On 230-kv tests at capacities from 554,000 kva to 2,259,000 kva, throughout a total of 16 tests, none of the pins fell. On the lighter tests, a high-speed moving-picture camera showed slight vibration of the pins, which vibration became less as the duty increased. On the heaviest tests, no vibration was found

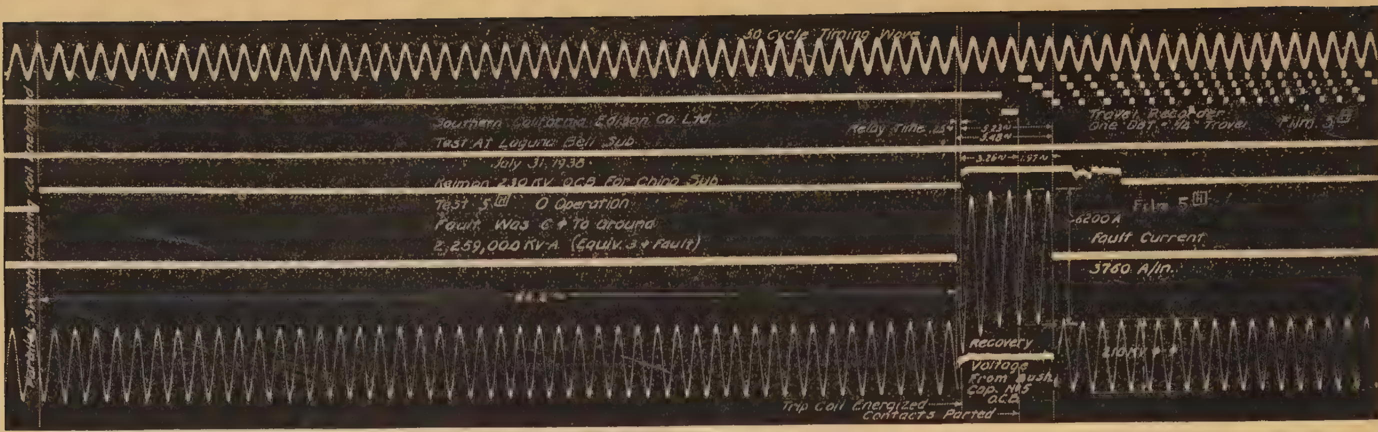
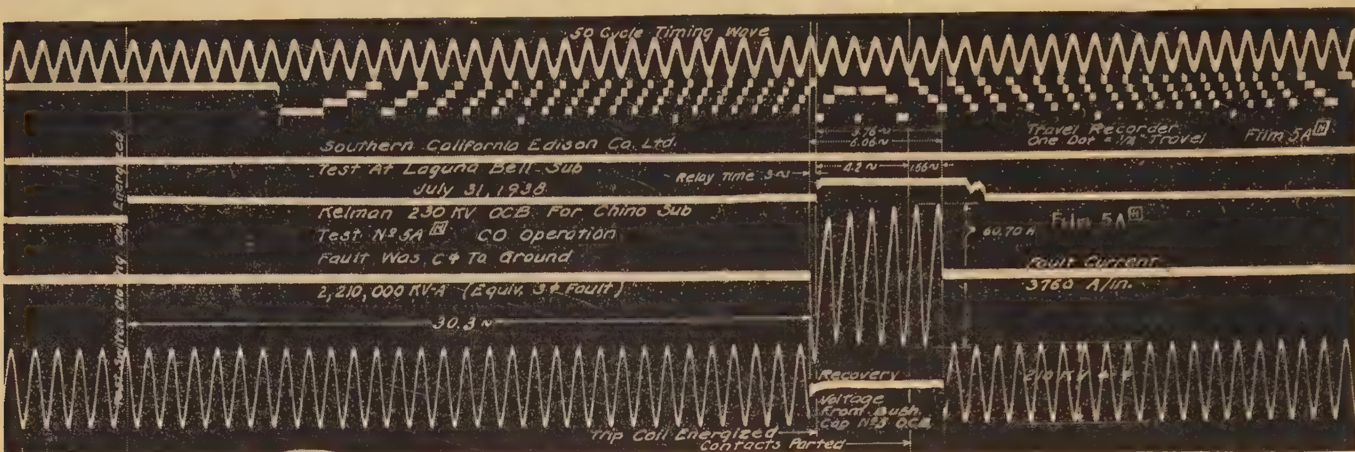


Figure 11 (above). Oscillogram of opening test at 2,259,000 kva

Figure 12 (below). Oscillogram of close–open test at 2,100,000 kva



shown because of the excessive arc length. It was discovered that these dethermalizers had too much clearance around the contacts and the ports were too much restricted, causing the oil to be forced out around the contacts giving practically no quenching effect. This difficulty was remedied, and the tests were completed on July 31, 1938.

It will be noted from these tables that as the duty increased the contact separation decreased at the time of arc extinction.

Oscillograph records were taken of these tests to give an accurate picture of the operation of the breaker. Figure 11 is an oscillogram of an opening operation with a full capacity short circuit at Laguna Bell substation. With the travel recorder it is possible to picture accurately the breaker action. From Figure 11 it can be seen that the arc was extinguished at exactly $^3/_4$-inch contact separation without any restrikes. Figure 12 is an oscillogram of a close–open operation of a full capacity short circuit at Laguna Bell substation. It can be readily seen from this figure that the prestrike was only $^1/_4$ inch, and contact separation at arc extinction was $^7/_8$ inch.

From these oscillograms and tables it should be noticed that dead time of the breaker was excessive. This was later corrected by a change in the tripping toggle.

It can be seen from the two oscillograms that no voltage disturbance occurred at arc extinction.

Since the Saugus tests were the first short-circuit tests to be made on the 220-kv Edison system, every precaution was taken to see that the 220-kv protection was in order and all switching was correct. The dispatching and switching were so well planned and the breaker functioned so perfectly that every test was carried out at the scheduled time without any system disturbance. This successful achievement allowed the privilege of future 220-kv short-circuit tests.

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A New One-Cycle Directional Overcurrent Relay

W. C. MORRIS
ASSOCIATE AIEE

Synopsis: This paper describes a new one-cycle directional overcurrent relay which is not limited in its application by lack of contact co-ordination. That contact co-ordination is important is indicated by the many incorrect relay operations which have been traced to the common practice of adding instantaneous attachment to directional overcurrent relays. For years it has been known that directional control is not sufficient for instantaneous directional overcurrent relays. This paper describes a new application of "memory action" which for the first time complements directional control to give always correct operation. The new idea is to use "memory action" to obtain a time delay in the operation of the overcurrent unit in the order of one-half cycle. Of course "memory action" is also used in the directional element to retain a high torque level even if the fault causes drastic voltage reduction. Both elements are of the rugged induction cylinder.

HIGH-speed relaying has become one of the most common means of permitting operation of a power system nearer its stability limit. Thus, extension of power transmission facilities has often been obviated. This, together with reduction of damage at the point of fault, has undeniably meant the saving of a large amount of critical material.

An important development in the high-speed relay family has been the distance relay with a stepped time–distance characteristic. With this type of relay, simultaneous instantaneous tripping is obtained for faults in the middle 80 per cent of the line section, and instantaneous tripping at one end for faults in the remaining 20 per cent. This is true over a large range of generating capacity.

For the protection of most of the lower voltage lines, distance relaying has generally been considered to be too expensive. On the other hand, it often occurs that the short-circuit currents of such lines are relatively fixed because the determining impedances are largely in the lines and transformers rather than in the connected generators. Thus, it is often possible to approach distance relay performance by means of instantaneous overcurrent relays. Generally, tripping must be allowed for faults in one direction only, thus necessitating directional action.

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W. C. Morris is design engineer, relay department, General Electric Company, Philadelphia, Pa.

Application Requirements

Any directional overcurrent relay, whether instantaneous or time delay, may operate incorrectly while the fault is being cleared or after it has been cleared, if the overcurrent unit is not prevented from closing its contacts when the fault is in the nontripping direction. Fundamentally, the cause of incorrect operation is the loss of a contact race. If the overcurrent unit has operated because of a fault in the nontripping direction, it must open its contacts before the contacts of the directional unit can close, under any condition causing reversal of the directional unit.

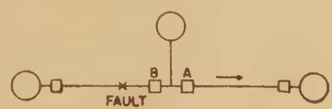


Figure 1. Basic system requiring correct directional action after fault is cleared

Figure 2. Basic system requiring correct directional action during sequence of circuit-breaker operation

Figure 1 shows a system where the directional overcurrent relay at A might trip the circuit breaker at A incorrectly after the fault is cleared, if the overcurrent unit is allowed to operate during the fault. If the normal load current is as shown by the arrow, and a fault appears at X as indicated in Figure 1, the contacts of the directional unit open, and the contacts of the overcurrent unit close. The instant that circuit breaker B opens, the current through the relay at A reverses and drops in magnitude. The directional unit closes its contacts quickly because it now has full voltage and load current applied to it. The overcurrent unit, on the other hand, opens its contacts slowly if the magnitude of the load current is near the drop-out value of the overcurrent unit. If the directional-unit contacts close before the overcurrent contacts can open, circuit breaker A will be tripped falsely even though circuit breaker B has already cleared the fault.

Figure 2 shows a system where a directional overcurrent relay might operate incorrectly during the sequency of circuit breaker operations to clear the fault, because of the fault current reversing when the first circuit breaker opens. For example, when the fault occurs, the overcurrent and directional units at 4 operate. If the generation at A is greater than that at B, the overcurrent unit at 2 may also operate, but circuit breaker 2 is not tripped because the fault current is in the direction to hold the directional-relay contacts open. When circuit breaker 4 opens, the current through circuit breaker 2 reverses causing its directional relay to reverse. If the directional-unit contacts close before the overcurrent-unit contacts can open, circuit breaker 2 is falsely tripped.

For simplicity, parallel lines were used in Figure 2. The same difficulty may arise in loop circuits.

Instantaneous directional overcurrent relays for phase protection have a contact

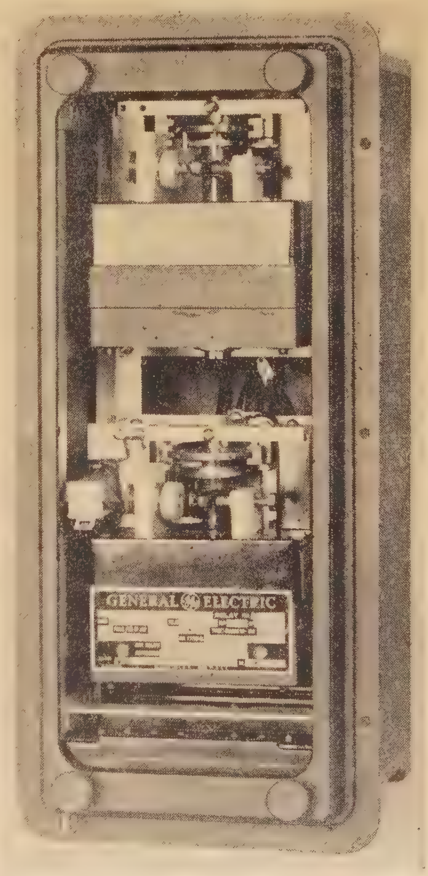


Figure 3. High-speed directional overcurrent relay

race which is not present either in time-delay directional overcurrent relays or in instantaneous directional overcurrent relays for ground-fault protection. This is the race between the contacts of the directional-unit opening and the overcurrent contacts closing at the inception of the fault. For example, if load in Figure 1 is flowing in the direction of the arrow, the directional-unit contacts at A are closed. When the fault appears at X, the instantaneous overcurrent-unit contacts of the same relay begin to close. At the same time, the directional unit is attempting to open its contacts because of the reversal of the direction of current flow. Obviously, if the contacts of both units are closed simultaneously during the transition, circuit breaker A is tripped falsely.

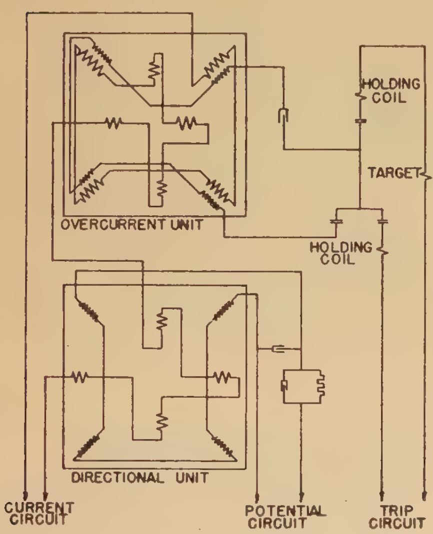


Figure 4. Internal connections of relay

Figure 5. Test oscillogram. Fault in non-tripping direction

Vibrator 1—Current in flux-shifting coils of overcurrent unit

Vibrator 2—Line current

Vibrator 3—Current in potential coil of directional unit

Vibrator 4—Voltage across potential circuit of directional unit

Vibrator 5—Current in trip circuit

Development of the Relay

In developing a high-speed directional overcurrent relay that operates correctly under any circuit condition, it is necessary to obtain proper contact co-ordination.

Directional control, as in the case of the time overcurrent relay, prevents incorrect tripping either after the fault is cleared or when there is a reversal of short-circuit current during the fault.

To win the more difficult contact race, namely, that occurring when a reverse current fault supplants load current in the tripping direction, it is necessary that the time required for the directional-unit contacts to open be less than the time for the overcurrent-unit contacts to close. This requirement suggests either that the directional unit should be fast even at very low voltage, or that the overcurrent unit should be delayed slightly. Actually, in order to increase the safety factor, both methods are employed. The overcurrent unit is delayed slightly by preventing the immediate rise of torque to its steady-state value. The operating time of the directional relay is decreased, principally at low voltage, by delaying the decay of current in the potential coil, retaining, at the same time, substantially, circuit frequency.

Description of the Relay

The relay consists of an overcurrent unit and a directional unit mounted in a single case. Figure 3 shows the relay, and Figure 4 shows the internal connections. In Figure 3, the upper unit is the overcurrent unit, and the lower unit is the directional unit. Both units are of the eight-pole induction-cylinder construction previously described.[1]

The main windings of the overcurrent unit consist of eight series-connected current coils.

As with all induction devices, the development of torque is dependent upon the difference in time phase between two or more fluxes. (This is not a complete explanation of the torque development of induction devices but is sufficiently explicit for present purposes.) Consequently, four of the current poles, in alternate position, have auxiliary coils. These windings provide a means of controlling or shifting the time phase of the air-gap flux of these poles. The four auxiliary coils, in series, are connected in series with a capacitor and one set of contacts of the directional unit. The torque of the overcurrent unit is proportional to the product of the square of the current and the sine of the angle between the fluxes produced in the shifted and unshifted poles. As long as the contacts of the directional unit are open, the fluxes in all poles are in phase and the torque of the overcurrent unit is zero. When the contacts of the directional unit close, the auxiliary coil circuit of the overcurrent unit is completed. This auxiliary circuit then shifts the phase angle of the flux in the corresponding poles so that it is no longer in phase with the flux in the other poles and the overcurrent unit has a torque in the direction to close its contacts.

The auxiliary circuit is an oscillating circuit, and the build-up of its current is delayed.[2] Since current in the auxiliary winding shifts the flux, a delay of the build-up of this current delays the flux shift. The build-up of current in the auxiliary winding is shown by the trace made by vibrator 1 of the oscillogram shown in Figure 6. This slow build-up

Figure 6. Test oscillogram. Fault in tripping direction

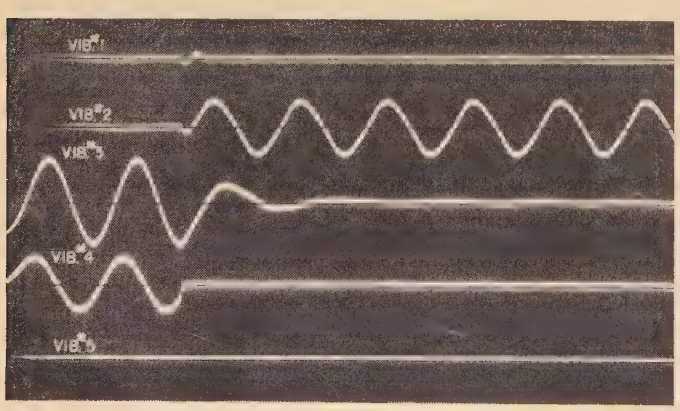
Vibrator 1—Current in flux-shifting coils of overcurrent unit

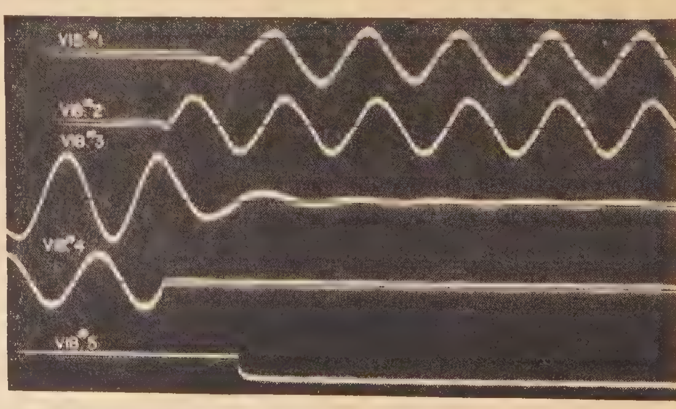
Vibrator 2—Line current

Vibrator 3—Current in potential coil of directional unit

Vibrator 4—Voltage across potential circuit of directional unit

Vibrator 5—Current in trip circuit





prevents the overcurrent unit from closing its contacts before the directional unit has time to open its contacts.

When the magnitude of the load current is near the pickup value of the overcurrent unit, the impulse that the overcurrent unit receives, under short-circuit conditions, before the contacts of the directional unit can open, will be sufficient to close the contacts of the overcurrent unit. The time required to close the contacts under this condition is greater than the time required to open the contacts of the directional unit, so that the trip circuit is not completed. This has been demonstrated by tests.

The windings of the directional unit consist of four current coils in series and four potential coils in series, mounted on alternate poles. The potential circuit has two capacitors and a resistor in addition to the coils. This potential circuit is also an oscillating circuit so that the flux in the potential coil does not immediately drop when the fault appears. The flux continues to follow system frequency for about one cycle which gives the directional unit ample torque to open its contacts quickly. This slow decay of the potential flux also gives the directional unit enough torque to close its contacts when the fault is in the trip direction even though the voltage across the potential circuit drops to a small value during the fault.

Figure 5 shows an oscillogram taken under the following conditions: Before the fault was applied, the directional relay had full potential. The current was in the direction to close the contacts of the directional unit but below the pickup value of the overcurrent unit. The instant the fault was applied, the current reversed and increased in magnitude to 13 times the pickup value of the overcurrent unit. Concurrently, the voltage across the potential circuit decreased to one per cent of rated potential. Vibrator 3 (Figure 5) shows how the current in the potential coils of the directional unit decayed. Vibrator 1 shows how the current in the auxiliary circuit of the overcurrent unit started to build up but was interrupted by the opening of the contact of the directional unit.

Figure 7. Time-current characteristic

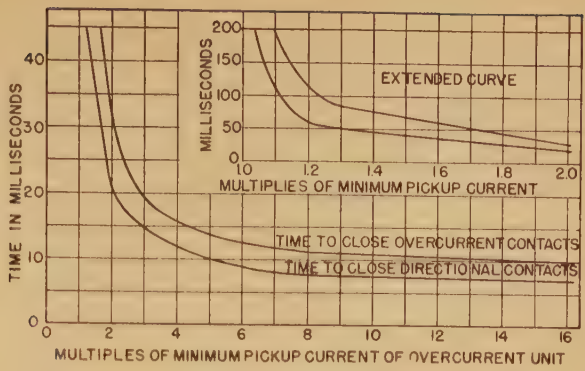


Figure 6 shows an oscillogram in which the conditions were the same as those described heretofore except that the current before the fault was in the nontripping direction and in the trip direction after the fault. This oscillogram shows the build-up of the current of the auxiliary circuit after the directional unit closed (vibrator 1). The oscillogram shows that the contacts of the directional unit closed to complete the auxiliary circuit in approximately one-half cycle (60-cycle basis). Then the overcurrent unit operated to close its contacts in approximately one-half cycle. The trip circuit is therefore completed in approximately one cycle. Since this relay is capable of operating in one cycle, it is believed that it can be called instantaneous, notwithstanding the AIEE definition of "instantaneous," which is "no intentional time delay."

Relay Characteristics

The directional unit of the relay has maximum torque when the relay current leads the relay voltage by 45 degrees. This means that, with the quadrature connection, the relay has maximum torque when the relay current lags the phase-to-neutral voltage of the same phase by 45 degrees. The directional unit will operate correctly at the maximum torque angle with one per cent of rated voltage and the minimum pickup value at which the overcurrent unit can be set.

The time to complete the trip circuit depends upon the direction of the load current before the fault appears. If the load current is in the trip direction, the contacts of the directional unit will be closed and the time to complete the trip circuit is the time to close the contacts of the overcurrent unit. If the load current is in the nontripping direction before the fault, the contacts of the directional unit will be open, and the time required to complete the trip circuit will be the time to close the contacts of the directional unit plus the time to close the contacts of the overcurrent unit. Figure 7 shows the time to close the contacts of the overcurrent unit at various multiples of minimum pickup current. Figure 7 also shows the time to close the contacts of the directional unit when the potential circuit voltage drops to one per cent of rated voltage and the current increases to various multiples of minimum pickup current of the overcurrent unit.

Summary

The relay will operate correctly on parallel lines or on a loop circuit, regardless of whether or not there is a possibility of a reversal of current during a fault or immediately following the interruption of the fault.

The directional unit will operate correctly and quickly when a fault appears even if the potential circuit voltage drops to as low as one per cent of rated voltage.

The overcurrent unit will operate only when the contacts of the directional unit are closed.

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The Automatic Welding-Machine Starter and Its Relation to Maximum Utilization of Power and Facilities

NOEL E. PORTER
ASSOCIATE AIEE

MANY electric-arc-welding applications are of such a nature that the welding machine is running with no load for a large percentage of the operating time. With this low utilization factor or "arc time," no-load power consumption becomes an appreciable item. In plants employing many machines there will be long leads between the machines and the work, either by necessity or for convenience. Under these conditions, the practicability of expecting the operator, or welder, to turn his machine on and off each time he plans to weld is certainly very remote. The automatic welding-machine starter has been developed to shut down the machine during the normal no-load running periods. Automatic starters have been in operation under average shipyard welding conditions for the past few months. Application of the automatic starters effects a very appreciable saving in power cost, about 25 per cent or more. In addition, a marked reduction in average demand on distribution systems is realized so that additional machines, up to 75 per cent, may be added without further power facilities expenditure.

The Apparatus and Its Operation

The automatic welding-machine starter is an electrical relay-control mechanism which may be connected to nearly any conventional single-operator a-c to d-c motor generator welding set. Operation of the unit is simple. When ready to strike an arc, the welder firmly touches his electrode, or rod, to the article to be welded, or work, and the welding machine starts. The machine continues to run as long as welding is in progress. When welding is finished, and after a predetermined delay period of no-load operation, the machine automatically stops. The cycle may then be repeated as often as is desired.

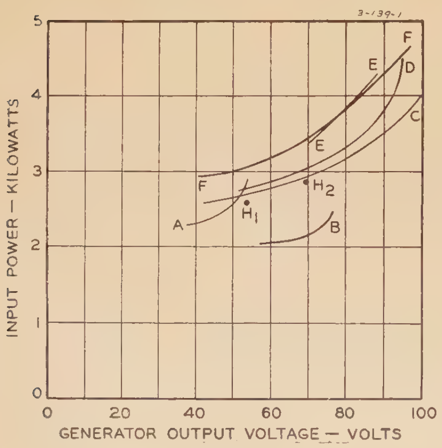

Figure 1. Welding-machine input power at no load

There are many methods of accomplishing the desired result from an electrical control standpoint. The apparatus must include the necessary relay and control components so that, when the electrode is touched to the work piece, that is, return or ground lead, thereby effecting an electrical contact, the machine will start and lock into the running position by the conventional line starter. Of particular interest is the time-delay relay as the no-load delay period directly effects power saving. A two- to three-minute interval is recommended to allow time for the welding machine to cool by ventilation. Moreover, according to welders themselves, two minutes should be allowed for changing rods and cleaning slag from the weld puddle. This prevents an unnecessary stop. Very short delay periods, such as one half a minute, increase the number of starts to a point where wear and tear on contacts is excessive. The automatic starter must be capable of immediately restarting, even at the instant the time-delay unit has called for stopping the welding machine. Any appreciable delay or erratic operation assignable to the electrical operation of the unit will have a detrimental psychological effect on the welder.

The numerous problems encountered in the development of a practical starter are mentioned but not analyzed here. Adaptability of the unit to various types and makes of welding machines, impedance of welding cable on steel plate, various contact resistances, the standstill impedance of welding generators,* and the requirements of various relays and component parts are matters of interest to the designer. A paper discussing these problems with circuit data will be prepared in the near future.

Relation to Power Cost

The no-load input for a group of popular welding machines is given in Figure 1. Nominal ratings are up to 400 amperes. Figure 2 is a plot of no-load power factor readings for the same group. The variation in input and power factor with output setting is expected and is a function of output voltage. Curves *C* and *D* represent machines with a wide range of open-circuit voltage. Less variation would be expected with machines of the more or less constant open-circuit voltage type, see curves *B* and *H*.

Power saving through the application of automatic starters is effected by many variables: the type of machine, type of welding,** output setting of the machine, the time-delay relay setting, and the cost of power.

Various power-saving tests were conducted. In all cases arc time was recorded. Arc time was measured with a synchronous time meter and relay control so that time was counted only when the arc was on. Power was measured with a graphic wattmeter for individual runs and by a watt-hour meter for banks of units. Graphic records provide other valuable data, such as the number of starts, duration of weld runs and off periods, and increase in power consumption with changes in output setting.

For arc times up to 35 per cent, the cumulative time of shutdown was found to vary between 6 and 14 hours on a 24-hour-day basis. The average centered about ten hours. The actual working-day period was checked from time-meter readings on welding machines without starters. This was found to be approxi-

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NOEL E. PORTER is electrical engineer at the Permanente Metals Corporation, Richmond yard one, Richmond, Calif.

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* The standstill impedance is the impedance measured across welding generator output terminals with generator at standstill.

** Power saving is effected by the type of welding that is, intermittency of welding, arc time, duration of weld runs, necessity for long delays because of fit-up, tacking, and so forth.

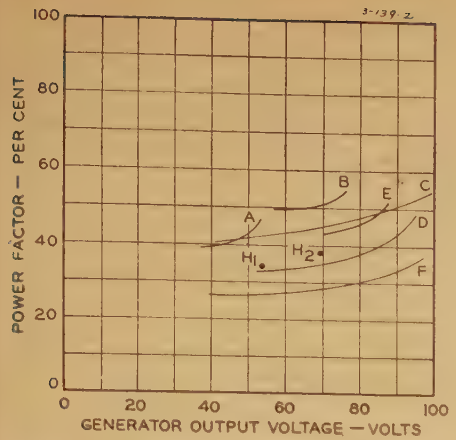

Figure 2. Welding-machine power-factor characteristics at no load

Table I. Power-Consumption Test

Test Number	1	2	3	4	5	6	7	8
Automatic starter connected during test	Yes	No	Yes	No	Yes	No	Yes	No
Test time (hours)	24	24	24	24	24	24	24	24
Power consumed by* four welding machines during test (kilowatt-hours)	277.7	284.1	199.1	378.1	305.0	462.3	315.0	497.5
Total number of arc hours of welding by four machines (hours)	20.99	15.66	19.8	23.96	26.53	26.29	23.8	27.0
Input per arc hour of welding (kilowatt-hours)	13.24	18.14	10.05	15.8	11.5	17.55	13.2	18.5
Average arc time, based on 21-hour work day (per cent)	25	18.6	23.6	28.5	30.4	31.3	28.4	32.1

Average values: Arc time .... 27.3 per cent
Kilowatt-hours per arc hour *without* starters 17.6
Kilowatt-hours per arc hour *with* starters 12.0
Saving in power with starters .... 31.7 per cent
Saving in power (kilowatt-hours per 24 hours per machine) .... 31.8 kilowatt-hours

* Average no-load input 3 kw, average load input 12-kw.

mately 21 hours. The departure from a 24-hour day is accountable to shift-change and noon-hour periods. The product of the no-load losses, shutdown time, and power cost will yield dollar value of the power saving.

Actual test data of a bank of welding machines with and without starters are given in Table I. The criterion for comparison is arc time.† Power readings were taken using a three-phase watt-hour meter.

Statistics and Average Power Input

Experience and logic tell us that a power circuit supplying an intermittent load will handle a much higher connected horsepower than the same circuit under continuous operating connected load. Welding machines present a highly intermittent load.

Applying the statistical theory of sampling, we can show that, with a circuit of four welding machines operating on 25 per cent average arc time, the probability is 0.42 that we will find three machines at no load and one machine welding, the probability is 0.315 that we will find all four machines at no load, and the probability is 0.265 that there will be more than one machine welding.

It must be assumed that load conditions on the four welding machines, at any interval, represent a sample taken from a grand lot of weld (load) and no-weld (no-load) intervals,* the average arc time of which is known to be 25 per cent. Since each welder operates individually and without regard to what the others are doing, there is some justification in assuming a normal distribution of the weld and no-weld intervals. The binomial law is then applied using the formula:

$$P=\frac{n!}{(n-c)!c!}P^{n-c}Q^{c}$$

This is simply the probability of finding c defectives in a sample of size n, where n is the number of welding machines, c is the number not welding, P is the fraction effective or average arc time (25 per cent effective in grand lot), and $Q=1-P$ the average not welding or fraction defective (the average not welding in the grand lot of weld and no-weld intervals).

If we increase the sample size,** n, or number of welding machines, it can be seen that factorial calculations become laborious. Moreover, if we wish to calculate the probability that there will be not more than a given number of machines welding, it is necessary to sum up a group of terms. Calculations then make use of the incomplete beta function for which charts have been prepared.[2]

Using charts on the incomplete beta function, we can show that for 100 welding machines operating on a 25 per cent average arc time, the probability is 0.955 (extremely probable) that there will never be more than 36 machines welding. Likewise, we can show that the probability is 0.900 (highly probable) that there will be not more than 30 machines welding for a given interval. This may be expressed differently, that is, for at least 90 per cent of the time there will not be more than 30 machines welding and not less than 70 machines at no load.

The statistical analysis is interesting and gives us a fair approximation of how close we may load a distribution system to the average load limit. As the number of machines is increased, the statistical analysis is more accurate, and average load values may more safely be used.

Relation to Maximum Utilization of Facilities

The relation of automatic starters to maximum utilization of facilities can best be emphasized through a hypothetical example, making use of certain reasonable assumptions. Consider a distribution system operating at capacity and supplying 100 welding machines at an average arc time of 25 per cent.‡ Average no-load input is three kilowatts and 40 per cent power factor. (See Figures 1 and 2.) From test data, the average welding input is 12 kw and 85 per cent power factor. If no automatic starters are employed, the average input will be:

$0.25\times100\times12$ kw $=300$ kw at 85 per cent power factor, 187 rkva, 355 kva
$0.75\times100\times3$ kw $=225$ kw at 40 per cent power factor, 516 rkva, 562 kva
Average input $=525$ kw, 877 kva, and 50.8 per cent power factor.

If the machines are operated on a 21-hour work-day period and the installation of automatic starters results with an average shutdown time of ten hours per day per machine. Converting hours to per cent time on a 25 per cent arc time basis, the average input will be:

$0.25\times100\times12$ kw $=300.0$ kw at 85 per cent power factor, 187 rkva, 355 kva
$0.274\times100\times3$ kw $=82.2$ kw at 40 per cent power factor, 188 rkva, 205 kva

Average input is 382.2 kw, 535 kva, and 71.5 per cent power factor. The application of starters effects a decrease 27.2 per

† For shipyards, 25 per cent arc time is believed to be a conservative average.

* From graphic instrument charts and impulse counter readings, it is found that the number of impulses, or times that a welding arc is struck, in a 24-hour work day will be in order of 2,000. This may substantiate the concept of weld and no-weld intervals and accordingly, statistical normality.

** It is sometimes convenient to calculate factorial n when n is not too large. Stirling's formula gives satisfactory results. The formula:

$$n=n^{n}e^{-n}\sqrt{2\pi n}$$

For $n=10$, the error is only 0.8 per cent.[1]

‡ The machines were welding, on the average, for 25 per cent of the time, or 5.25 hours on a 21-hour-work-day basis. They were off, on the average of ten hours, and running at no load on the time-delay relay period for the balance of the time, 5.75 hours, which is 27.4 per cent of the period.

Table II

Item	Method 1 Additional Capacity	Method 2 Power-factor Correction	Method 3 Starters
1. Facilities cost	$ 7,900.00 (a)	$ 8,730.00 (b)	$ 9,600.00 (c)
2. Power cost, per month for 160 machines	3,933.00	3,601.00	2,775.00
3. Original power cost per month for 100 machines	2,460.00	2,460.00	2,460.00
4. Additional power cost per month for operating 60 machines (item 2 minus 3)	1,473.00	1,141.00	315.00
5. Additional power cost for 6 months period	8,850.00	6,846.00	1,890.00
6. Cost of facilities and power for 6 months (item 1 plus 5)	16,750.00	15,576.00	11,490.00
7. Cost per machine per month for 6 months period (not including machine cost)	279.00	260.00	191.00
8. Per cent of unit machine cost @ $450.00 per unit	62 per cent	27.7 per cent	42.5 per cent
9. Continued power saving per month over method 1	None	332.00	1,158.00

(a). 550 kva of additional capacity at $15.00 per kilovolt-ampere installed.

(b). 873 kva of correction required at $10.00 per kilovolt-ampere installed.

(c). 160 starters at $60.00 each installed. No allowance is made for maintenance in method 3. This item must ever be recognized where moving parts, that is, relays, and so forth, are employed. It is believed, however, that the maintenance cost for starters would be at least offset by the decreased maintenance cost on the welding machines themselves, because, on account of the starters, their total running time will be greatly reduced.

cent in average kilowatt demand, 39 per cent in average kilovolt-ampere demand, 46.7 per cent in average reactive-kilovolt-ampere demand, and an increase of 11.7 per cent in average power factor. With power at $0.01 per kilowatt-hour, on a 21-hour work-day period, this amounts to approximately $900 per month. If the installed cost is $60.00 per unit, starters will pay for themselves on power savings alone in less than seven months. The average reactive demand is reduced by 328 reactive kilovolt-amperes. In equivalent capacitance correction, at $10.00 per kilovolt-ampere installed, this represents more than half the first cost of the starters.

In the example, application of automatic starters allows for an increase of 64 per cent in capacity or 64 additional welding machines on the already loaded distribution system. Higher arc times will increase the average input and reduce the additional capacity allowance, and vice versa. Power saving and investment value will depend upon power rates and schedules. The saving will increase where power is sold on some form of kilovolt-ampere-hour basis.

Suppose in this hypothetical case, it was decided to purchase 60 additional welding machines at a cost of $450.00 each. Since the distribution system is operating at capacity, power supply for the new machines becomes a serious problem. There are three solutions to the problem:

1. Installation of additional distribution capacity.

2. Power factor correction of the existing system.

3. Application of automatic welding machine starters.

It may be desirable to write off the expenditure in a short period such as six months.

Using a typical rate, power cost is $0.007 per kilowatt-hour, and the total charge per month is decreased or increased, respectively, by 0.25 per cent for each one per cent average power factor above or below 85 per cent. Estimates, neglecting interest, depreciation, and items common to all methods, are given in Table II.

A continued power saving of $332.00 per month by method 2 and $1,158.00 per month by method 3, over method 1, will be realized. Longer investment periods will naturally place the balance more and more in favor of the automatic welding-machine starters. It is believed that the figures used and shorter period are in keeping with many wartime expansion policies. It is of interest to note that a combination of capacitance correction and starters would allow for more than doubling the capacity of the original loaded system in the example.

Conclusions

The automatic welding-machine starter has been in operation under average shipyard welding conditions for a sufficient length of time to prove its practicability and merits. Application of automatic starters offers two principal advantages:

1. A very recognizable saving in power cost is effected because the welding machine is automatically shut down for the greater part of its normal no-load operating time. Test results show this saving to be in order of 25 to 30 per cent of the total power consumed for average arc times of 25 per cent and with a three-minute delay setting on the automatic starter. The saving will vary with different welding application and will increase as the average arc time is reduced and with shorter time-delay relay settings. Since the no-load input for a-c to d-c motor–generator welding sets is inherently of a low power factor nature, application of automatic starters greatly reduces the average reactive-kilovolt-ampere demand (in the order of 50 per cent reduction), so that the average kilovolt-ampere input is reduced far more than the average kilowatt input. The economic advantage of automatic starters on a purely power cost saving analysis is entirely dependent upon power rate schedules and will be more favorable where power is sold on some form of kilovolt-ampere-hour basis.

2. Probably the most important advantage offered by the application of automatic starters is that, by reducing the average kilowatt and kilovolt-ampere demand, appreciable increase in capacity on existing distribution systems may be realized. The increased capacity allowance will vary from 25 per cent to 75 per cent, depending upon arc time and other conditions, and may be over 100 per cent in cases where starters are used in combination with capacitance correction.

It is believed that the application of starters will warrant consideration where new or additional distribution facilities are contemplated. In general, starters will show a greater economic advantage over any other method of reducing overall power and facilities cost and, in any case, will provide for the maximum utilization of welding power distribution systems.

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2. The Engineers' Manual of Statistical Methods, Leslie E. Simon.

Lightning Surges Transferred From One Circuit to Another Through Transformers

P. L. BELLASCHI
FELLOW AIEE

IT is generally recognized that through the medium of transformers surges can be transferred from circuits exposed to lightning to circuits considered otherwise nonexposed. The latter may connect to rotating machines or to industrial apparatus of inherently low-impulse insulation level. On these seemingly nonexposed circuits may be found apparatus of old design which as a rule are even more vulnerable to surges. In these problems the question is to determine

1. The extent apparatus may be endangered.
2. Whether the relative risk incurred weighed against the importance of the installation and the cost of damage, warrants protective equipment.

In each case sound engineering recommendations demand a fairly close estimate of the transferred surges.

In examining cases of this character in addition to published data, the writer has found particularly helpful the results of an investigation at Sharon in 1932–33. Seasoned by experience, these results and the simplified methods derived from them are presented here to assist in a practical way those engaged in this type of protection studies. This contribution should serve besides to call attention again to the vital importance transferred surges may assume under certain conditions.

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P. L. Bellaschi is development engineer with Westinghouse Electric and Manufacturing Company, Sharon, Pa

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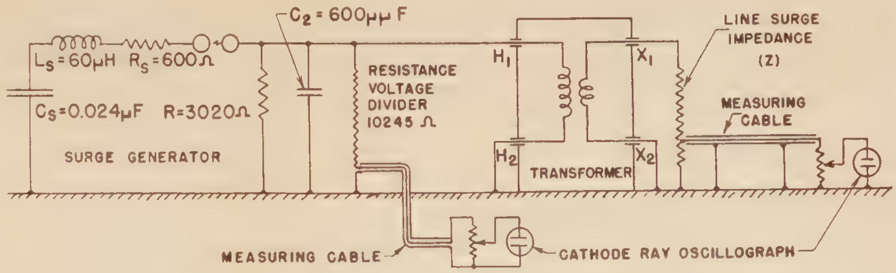
The Nature of the Problem

Contributions on the mechanism governing the transfer of surges through transformers have been reported by various investigators.[1–5] Concurrently, the protection of rotating machines and similar low-impulse-strength apparatus subject to these surges has been discussed.[6–11] Essentially, when a lightning surge is impressed on one winding of a transformer, a surge appears at the terminals of a second winding. The surge thus transferred to other windings and connected circuits consists respectively of an electrostatic component and an electromagnetic component. The electrostatic component results from the capacitance coupling between the windings and to ground (Figure 13), while the electromagnetic component depends on the turn ratio and the short-circuit inductance between the two windings (Figure 15). The terminal conditions naturally contribute also to determine the amplitude, the wave form, and the duration of the surge transferred. Superimposed on these components will be present in lesser or greater amounts the effects due to oscillations from the windings. All these factors will be apparent from the tests and the discussion which follow.

One Series of Tests

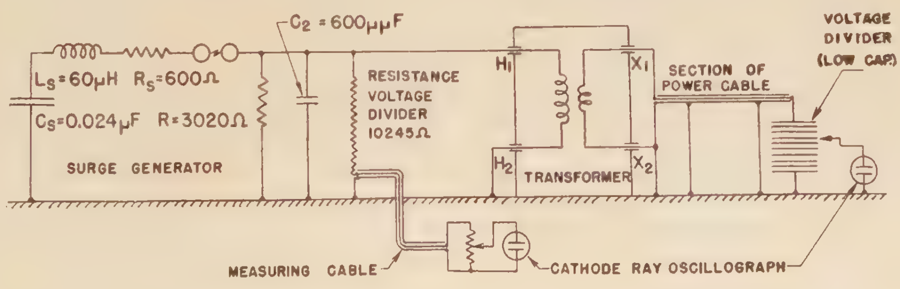
All tests and the corresponding measurements were conducted in the manner illustrated in Figure 1. In this series of tests the transformer is a single-phase two-winding core-form unit, rated at 1,000 kva and 76,210/2,300 volts. The turn ratio is 33.1 to 1. Other characteristics of the transformer are listed in Table I. The terminal arrangements cover a wide range of the practical conditions encountered in service. These are summarized in Figure 2. The voltage applied

Figure 1. Typical arrangements and method of test



A (above). Low-voltage terminal of transformer connected to line

B (below). Low-voltage terminal of transformer connected to section of cable



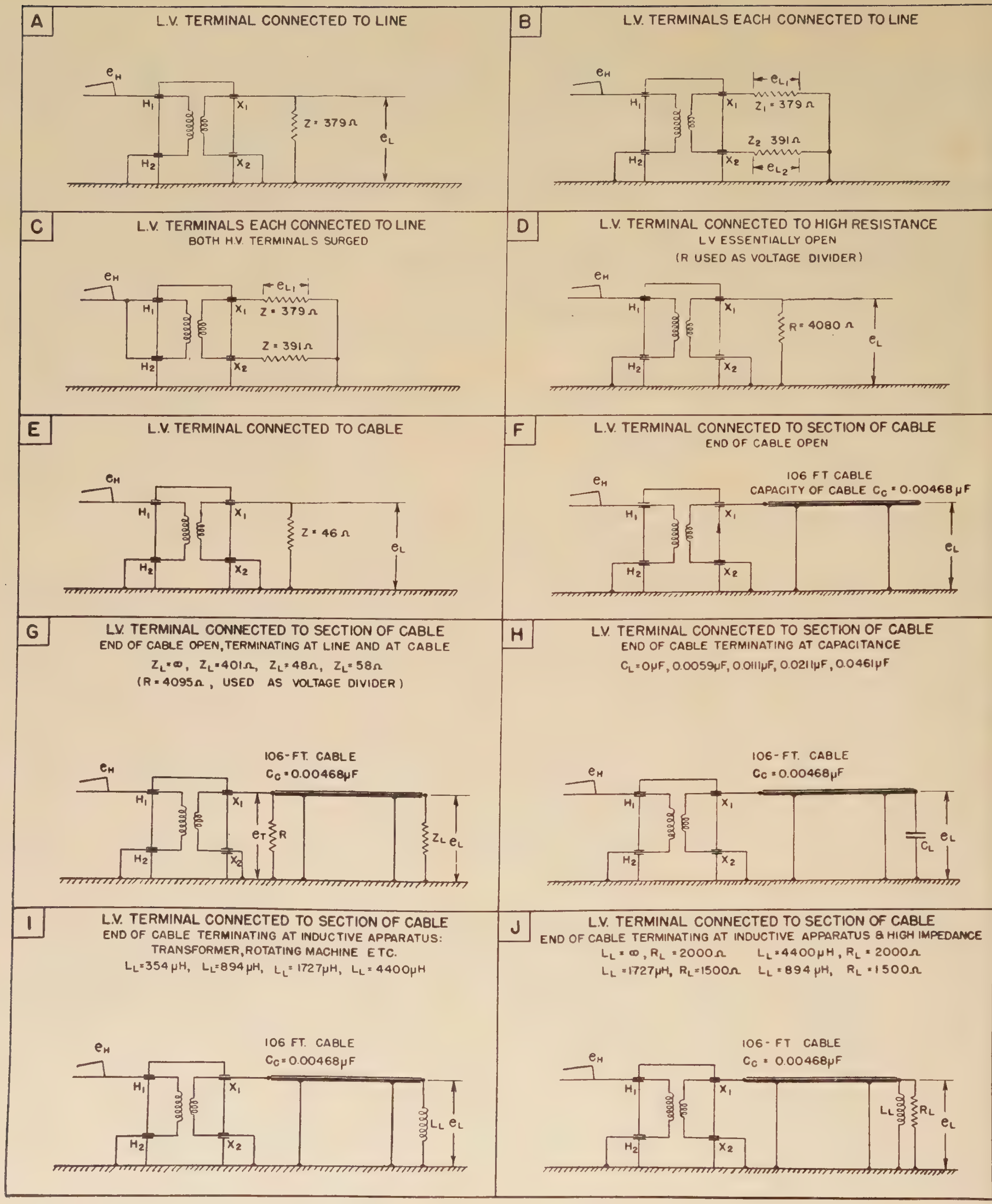


Figure 2. Terminal conditions for tests with 1,000-kva 76,210/2,300-volt single-phase core-form transformer

to the high-voltage winding (e_H) measures 364-kv crest, and in this too the tests have the significance of approaching the conditions of interest in actual service. Likewise, the surge applied corresponds quite well to the standard $1^1/_2$x40-microsecond wave.

For test condition *A*, one low-voltage terminal connects to an impedance $Z=379$ ohms, the equivalent of a long overhead line. The other terminal goes directly to ground. The surge voltage e_L transferred to the low-voltage terminal and to the line is recorded in Figure 3. In effect it rises to crest in eight microseconds, and recedes on the tail in about the same time as the wave applied. The

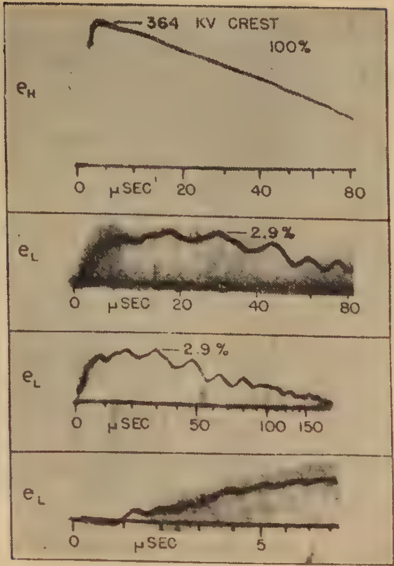
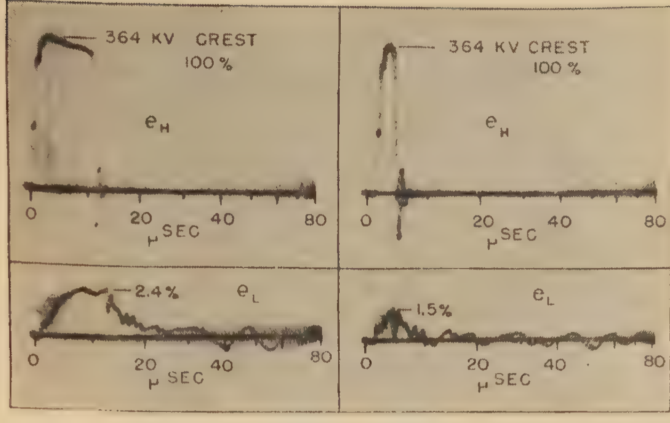


Figure 3. Oscillograms for test condition *A* (Figure 2)

electrostatic component accounts in part for the front. Beyond crest the electromagnetic component plus the superimposed oscillations predominate. The crest value 2.9 per cent corresponds essentially to turn ratio (100 per cent divided by 33.1). Test *A* was repeated with chopped waves applied. From Figure 4, as it should be expected, the surge e_L transferred to the line practically disappears for increasingly shorter impulses e_H.

In test *B*, each of the low-voltage terminals connects to a line impedance. In this case, the electromagnetic component divides about equally between the two lines, as in e_{L_1} and e_{L_2} of Figure 5. Note also the opposite polarity with respect to ground of the two. Only on the low-voltage terminal, physically opposite H_1, does the electrostatic component occur. In test *C*, surge e_H is applied simultaneously to both high-voltage terminals, so that in this case the electromagnetic component in e_L (Figure 5), disappears. Here the electrostatic component (1.3 per cent) is followed by the natural oscillations set up in the winding. In test *D*, the high resistance practically amounts to an open low-voltage terminal. Under this condition, because of the full development of the electrostatic effect and similarly because of the rapid development of the electromagnetic effect, the surge e_L (Figure 5) rises more abruptly and reaches a higher crest (3.7 per cent) than in any of the previous tests. The bottom oscillogram e_L of Figure 5 shows the influence of a long cable. This condition corresponds to *E*. As the result of the low impedance of the cable, the electrostatic effect practically has vanished insofar as appearing at the terminal. The surge transferred to the cable rises to crest in a much longer time than for the overhead line because of the greater time constant L_S/Z, attaining 2.1 per cent crest as against 2.9 per cent for the line.

Figure 4. Oscillograms for test condition *A* (Figure 2)



In service transformer terminals may connect to a section of cable which, under certain conditions, may remain open in the manner of test *F*. In this case, as recorded in Figure 6, the cable voltage oscillates, reaching 5.2 per cent, which is nearly twice the voltage e_H divided by the turn ratio. The oscillations are largely between the short-circuit inductance (L_S) and the cable capacitance (C_C), though in part other winding effects enter into play. A chopped wave e_H results in a transferred surge of the character shown.

Under other service conditions, the section of cable may connect directly to an overhead line or continue with another cable for a great extension. The voltages developed under these conditions (test *G*) are shown in Figure 7. For either the long line or extensive cables the section of cable is not a major influencing factor.

The oscillograms show that the voltages at the two ends of the cable section are much the same.

In test *H*, capacitances were connected to the open end of the cable. The capacitances applied in effect are equivalent to extending the 106-foot cable respectively to 240, 360, 580, and 1,120 feet. The results are recorded in Figure 8. The period of the transferred surge naturally increases with the capacitance, but the amplitude remains practically unchanged at 5.1 per cent.

Another practical case occurs where the transformer supplies inductive apparatus such as other transformers or rotating machines. This condition is approached in test *I*. On the basis that the short-circuit inductance (L_S) of the transformer is 100 per cent, the inductances L_L applied are respectively 26, 66, 128, and 325 per cent which values correspond to a wide range of effective loads. The oscillograms of Figure 9 show that both the amplitude and the period of the trans-

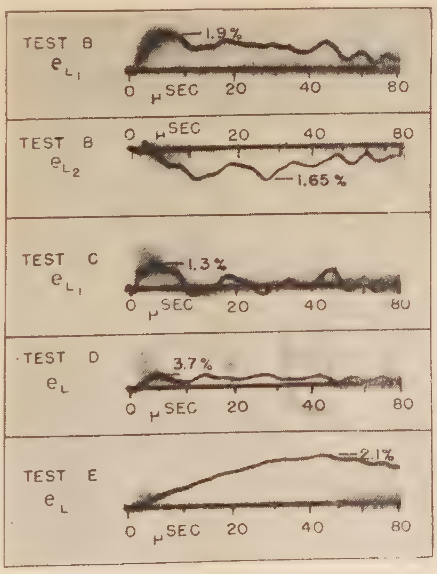


Figure 5. Oscillograms for test conditions *B*, *C*, *D*, and *E* (Figure 2)

Applied voltage e_H as in Figure 3

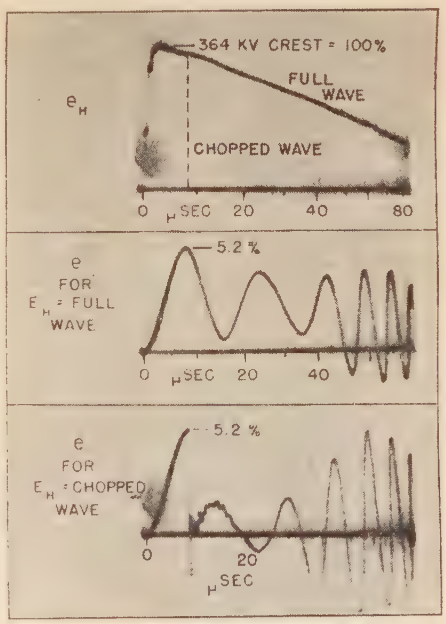


Figure 6. Oscillograms for test condition *F* (Figure 2)

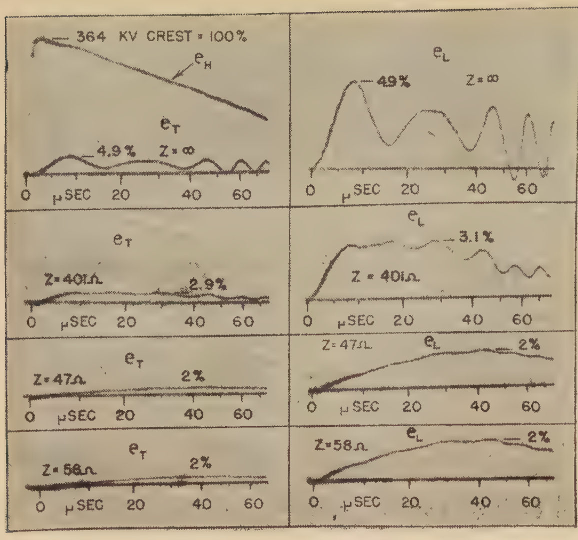


Figure 7. Oscillograms for test condition *G* (Figure 2)

ferred surge decrease with decreasing inductance load.

Another set of load conditions is the combination of high surge impedances (resistances) and inductances in *J*. The corresponding transferred surges are recorded in Figure 10. These tests complete the investigation on the 1,000-kva transformer.

Other Tests and Experiences

In Figure 11 are recorded the voltages transferred through a single-phase 4,500-kva 144,000/12,000-volt, shell-form transformer. The turn ratio is 12 to 1.

Table I. Design Characteristics of Transformers Used in Tests

1. Core-form, oil-immersed, self-cooled, 1,000-kva, 76,210/2,300-volt, single-phase, 60-cycle
   Number of turns: high-voltage, 3,576 turns
   low-voltage, 108 turns
   Turn ratio = 33.1
   Impedance = 9.62 per cent
   Short-circuit inductance (referred to low voltage) = 0.00135 henry
   Short-circuit inductance (referred to high voltage) = 1.48 henrys
   Construction, two-legged design
   High-voltage winding, pancake coils, one column each leg
   Low-voltage winding, two layers on each leg adjacent to core
2. Shell-form, oil-immersed, self-cooled, 4,500-kva, 144,000-120,000/12,000-volt, single-phase, 25-cycle
   Number of turns: high-voltage, 1,582 turns
   low-voltage, 132–158 turns (taps)
   Turn ratio (in tests) = 12
   Impedance = 4 per cent
   Short-circuit inductance (referred to low voltage) = 0.008 henry
   Short-circuit inductance (referred to high voltage) = 1.15 henrys
   Construction, four high–low interleaved (see Figure 13A)
   Two high-voltage groups each 791 turns
   Low-voltage center group 62–88 turns (taps)
   Two low-voltage end groups each 35 turns
3. Shell-form model, oil-immersed, special
   No rating assigned
   Turn ratio = 6.14
   Construction (see Figure 12)
   High-voltage coil group 2,180 turns
   Low-voltage coil *A* 18 turns
   Low-voltage coil *B* 337 turns

Other characteristics of this transformer are listed in Table I. In these tests the low-voltage winding in effect connects to a long overhead line, while the impulse voltages applied to the high-voltage winding reach practically the full test level of the insulation. The electrostatic component is clearly in evidence for the first ten microseconds; beyond this time the electromagnetic component with some superimposed oscillations of minor import is the dominating factor. In the chopped-wave test, the electromagnetic component practically disappears. The tests

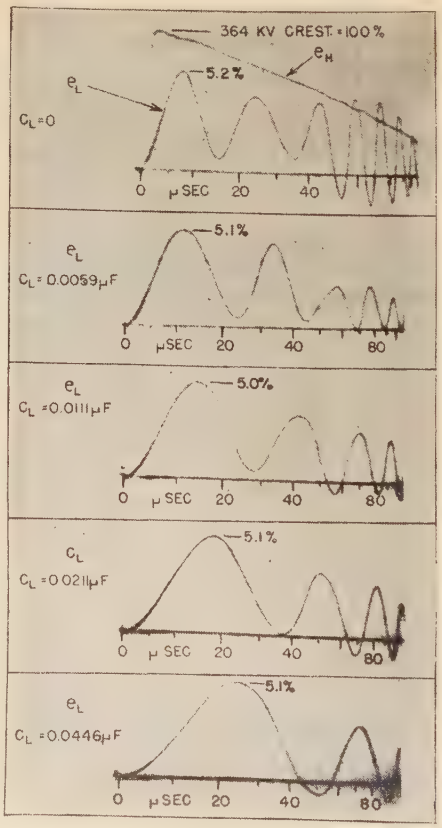


Figure 8. Oscillograms for test condition *H* (Figure 2)

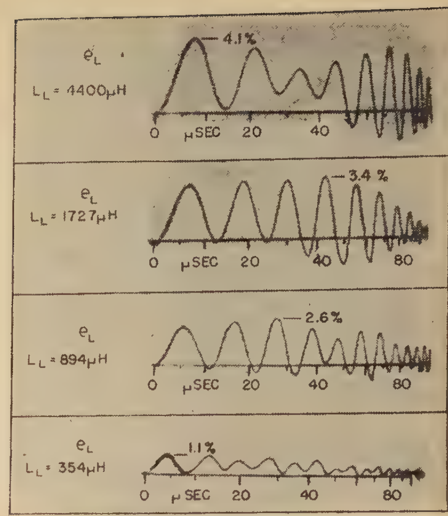


Figure 9. Oscillograms for test condition *I* (Figure 2)

Applied voltage e_H as in Figure 3

were repeated with a 100-foot section of cable connected to the low voltage as in *B* of Figure 1. In these tests the crest voltage developed was sufficient to flash over the cable at the end.

In the course of the many commercial impulse tests applied to transformers at Sharon, the effects of surges transferred through windings have been much in evidence. Bushings of windings not under test often require protection from flash-over. In this connection, it is well to point out that unusually high surge voltages can be transferred to high-voltage terminals when low-voltage windings of high insulation level are being tested. For instance, a 110-kv full-wave surge applied to the low-voltage windings of a 6,600/132,000-volt power transformer (ratio 20 to 1) is certain to flash over the high-voltage bushing.

The experience from commercial im-

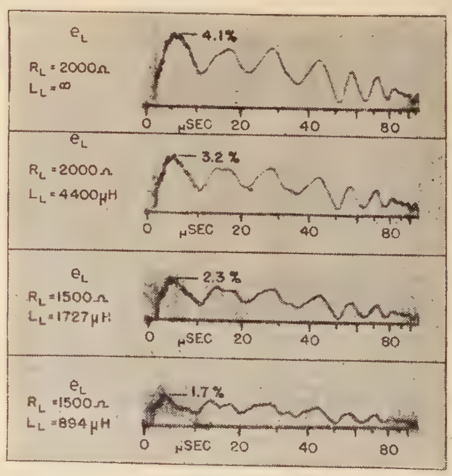


Figure 10. Oscillograms for test condition *J* (Figure 2)

Applied voltage e_H as in Figure 3

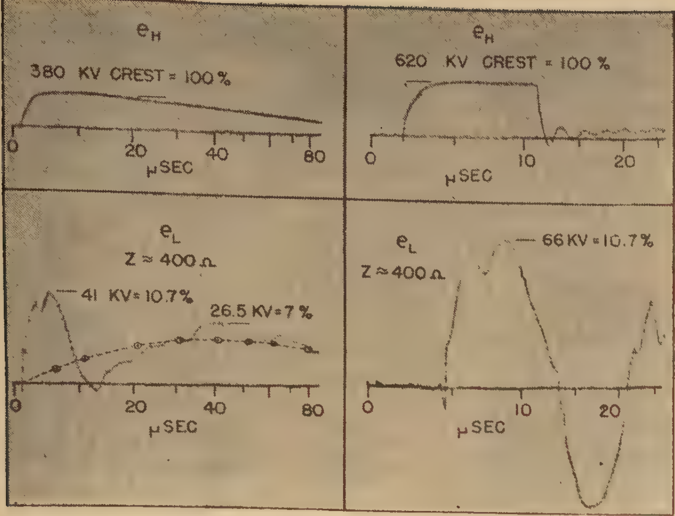
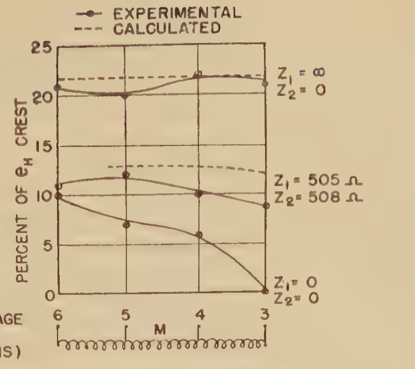


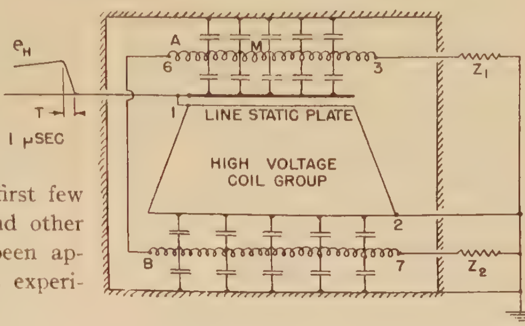
Figure 11. Oscillograms for test condition *A* (Figure 1) with 4,500-kva 144,000/12,000-volt single-phase shell-form transformer

Circled dots are calculated values of electromagnetic component

pulse tests at Sharon bears out also that surges may be transferred through a number of transformers in succession. In the commercial tests, power excitation is supplied from a 5,000-kva, 2,300-volt generator of old design through a 2,000-kva 2,300/6,900-volt transformer, a 1,000-foot cable, and again through another 2,000-kva transformer which serves to adjust the 6,900-volt cable supply to the primary voltage of the transformer under impulse test. Depending on the nature of the commercial impulse test, transferred surges of various intensities would reach the rotating machine. These seriously damaged the machine windings on three occasions in the course of the first few years. Suitable capacitances and other protective devices have since been applied and no further difficulties experienced.

Figure 12 (right). Electrostatic effect—calculated and test data for shell-form model





Electrostatic Component and Oscillations

The factors which affect the electrostatic component are several. These are discussed here in simple practical terms with reference to the typical examples in Figures 12 and 13. The electrostatic voltage induced in the low-voltage winding depends upon the capacitance relation between the high and low-voltage windings and to ground. In turn, the voltage induced in the low-voltage winding is discharged through the inductance of part of the winding into the terminal impedance. Thus from the simplified circuit in *C* of Figure 13 for the abrupt surge e_H,

$$e_L \approx \frac{C_1}{C_1+C_2} \cdot \frac{Z\epsilon^{-\frac{Z}{2L}t} \sin t\sqrt{\frac{1}{LC_2}-\frac{Z^2}{4L^2}}}{\sqrt{\frac{L}{C_2}-\frac{Z^2}{4}}}\, e_H \tag{1}$$

The calculated values from this simplified approach are in fairly good agreement with the test results. A more rigorous analysis requires that account be taken of the additional circuit elements shown in Figures 12 and 13 but in so doing the solution becomes quite complex.

Simply stated, the electrostatic component depends on the ratio, Z divided by $\sqrt{L/C_2}$, and on the length of the front of surge e_H with respect to the natural period $2\pi\sqrt{LC_2}$. In the case of a long cable where Z is small or for a segment of cable of large capacitance, the electrostatic component disappears. The electrostatic component needs be considered only where the low-voltage winding connects to aerial lines, to highly inductive apparatus or machinery, to loads of effectively high impedance, and where the capacitance to ground is small. The physical characteristics of the transformer windings are a factor, but these do not bear any simple relation in terms of the usual design constants. The ratio of the insulation test levels of the windings is a measure of the factor $C_1/(C_1+C_2)$ and, other things being equal, of the relative

Figure 13. Electrostatic effect—calculated and test data for 4,500-kva shell-form transformer

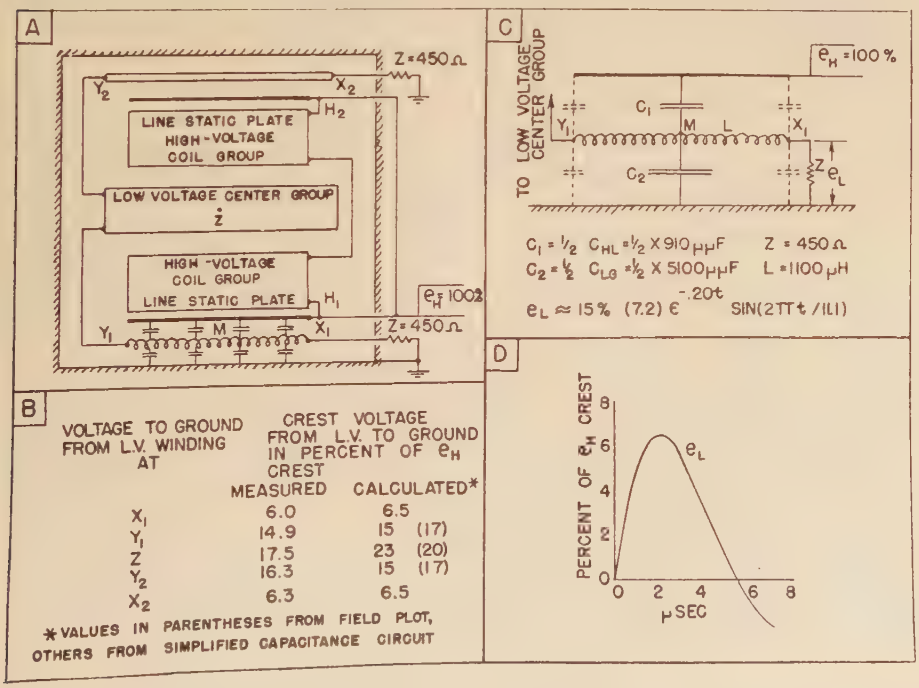


Table II. Electromagnetic Component—Comparison of Calculated Data With Tests for 1000-Kva Core-Form Transformer

Test Condition	Terminal Impedance	Crest Voltage e_L in Per Cent e_H Crest: Calculated*	Crest Voltage e_L in Per Cent e_H Crest: Experimental	Wave Form
Figure 2A	Z_L=379 ohms	2.6	2.9	See Figure 14 for comparison of calculated* and recorded wave form
Figure 2E	Z_L= 46 ohms	1.6	2.1	
Figure 2F	C_C=0.00468 microfarad	5.8	5.2	
Figure 2H	C_C+C_L=0.0493 microfarad	5.2	5.1	

* Data calculated from equations 2 and 3, transformer constants in Table I, and terminal impedances in column 2 above. Wave form of e_H in calculations approximately as in tests.

amplitude of the electrostatic component. The calculations and tests in Figures 12 and 13 and in the preceding oscillograms are quite indicative of the amplitude and duration of the electrostatic components expected in general.

As it may be seen from the oscillograms, the electrostatic impact sets the low-voltage winding oscillating. In addition oscillations in the high-voltage winding are induced electromagnetically in the low-voltage winding, and these in turn appear in the transferred surge. Simplified calculations of these effects are beset with difficulties. In general, however, the superimposed oscillations are not of primary importance.

Table III. Comparison of Period of Oscillation for Various Cable Capacitances

Short-Circuit Inductance (L_S in Microhenrys)	Cable Capacitance (C_O in Microfarads)	Period of Oscillation: Calculated ($T=2\pi\sqrt{L_S C_O}$) (Microseconds)	Period of Oscillation: Experimental (Figure 8) (Microseconds)
1,350	0.00468	15.8	16
1,350	0.0106	24.8	23
1,350	0.0158	29.0	28
1,350	0.0258	37.0	38
1,350	0.0493	51.3	52

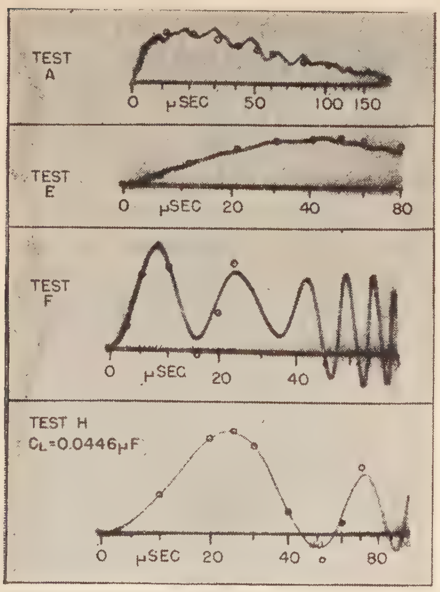


Figure 14. Electromagnetic effect—calculated (circled dots) and test waves of e_L for 1,000-kva core-form transformer

Test conditions *A*, *E*, *F*, and *H* (Figure 2)

Electromagnetic Effect

Surge Transferred to a Long Line or Cable

The general solution for the electromagnetic component is treated in the appendix. From a practical standpoint, refer to the equivalent simplified circuit of Figure 15D and let

$e_H = E\epsilon^{-\alpha t}$ = impulse voltage applied to high-voltage winding
L_S = short-circuit inductance of transformer (referred to low-voltage side)
N = turn ratio
Z = surge impedance of line or cable

Then the electromagnetic component transferred to the line or cable is

$$e_L = \frac{Z}{Z-\alpha L_S}\left[\epsilon^{-\alpha t} - \epsilon^{-\frac{Z}{L_S}t}\right]\frac{E}{N} \qquad (2)$$

In Figures 11 and 14 and in Table II are compared the calculations for three typical cases with the corresponding experimental data. It is apparent that for these conditions the electromagnetic component is conducive to fairly good predetermination. The preceding equation can be adapted readily to similar conditions such as those shown in tests *B*, *C*, and *D* of Figure 2. The behavior for shorter lengths of cable and line is considered in the following section.

Surge Transferred to Section of Cable (or Line)

For this condition the simplified circuit of Figure 15E applies. Let the applied voltage $e_H = E\epsilon^{-\alpha t}$, then

$$e_L = \left[\frac{\epsilon^{-\alpha t}}{1+\left(\frac{\alpha}{\omega}\right)^2} + \frac{\sin\left(\omega t - \tan^{-1}\frac{\omega}{\alpha}\right)}{\sqrt{1+\left(\frac{\alpha}{\omega}\right)^2}}\right]\frac{E}{N} \qquad (3)$$

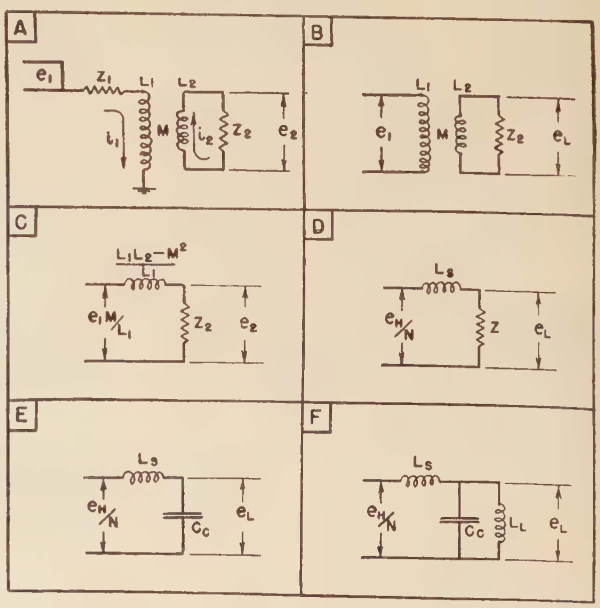
where $\omega = 1/\sqrt{L_S C_C}$ and C_C = capacitance of cable section.

On the whole, the comparison between the calculated and experimental data in Figure 14 and in Table II for the 106-foot section of cable is good. Equation 3, however, does not take into account the attenuation due to losses, nor does it consider the capacitance of the windings and other effects which apparently are responsible for the "beat-note" phenomena present in the oscillations.

As shown in the oscillograms of Figure 7, the cable section terminating in a lumped impedance equal to the surge impedance of the cable behaves essentially as a continuous long cable. In the case where the cable section connects to a long overhead line, the cable voltage no longer is oscillatory. The reason for this lies in the fact that the line surge impedance (Z=401 ohms) appears in Figure 15E as a circuit element shunting the cable capacitance (C_C=0.00468 microfarad), and its value approaches closely the

Figure 15. Typical circuits for surges transferred through windings

Simplified circuits *D*, *E*, and *F* represent low-voltage terminal connected, respectively, to long line or cable (Z), to cable section (C_C) and to cable section (C_C) terminating at induction apparatus (L_L)



critical damping impedance $^1/_2\sqrt{L_S/C_C}$= 270 ohms.

In test H of Figure 2, the load capacitances amount in effect to extending the cable section. The surge transferred to these sections is given by equation 3 where the cable capacitance in the equation now becomes $C_O=C_C+C_L$. In Table III are compared the calculated and experimental periods of oscillation for the various cable capacitances.

In Figure 14 and Table II are compared the calculated and experimental data of the transferred voltage for $C_L=0.0446$ microfarad. This load capacitance is equivalent to extending the cable to 1,120 feet. In an actual section of this length, the cable would have an inductance less than ten per cent of L_S. For a much longer section, it is apparent that, besides the capacitance, the inductance of the cable need be considered. That is, in Figure 15E for segments of cable where the total inductance $L_C=Z^2C_C$ (constants of cable) is small relative to the transformer short-circuit inductance L_S, the cable may be treated as a lumped capacitance. For longer sections where the cable inductance becomes appreciable but is not excessively large compared to L_S, the cable may be replaced with good approximation by its equivalent π circuit. For still longer sections of cable (or line), transferred surges should be dealt with in terms of traveling-wave phenomena.

Surge Transferred to Section of Cable and Terminating Inductive Apparatus

For this condition the simplified circuit is given in Figure 15F. The capacitance C_C includes the capacitance of the connecting apparatus, or, in the absence of a cable section, it represents the capacitance of the apparatus alone. For an applied surge $e_H=E\epsilon^{-\alpha t}$, the transferred surge is

$$e_L=\left[\frac{\epsilon^{-\alpha t}}{1+\left(\frac{\alpha}{\omega}\right)^2}+\frac{\sin\left(\omega t-\tan^{-1}\frac{\omega}{\alpha}\right)}{\sqrt{1+\left(\frac{\alpha}{\omega}\right)^2}}\right]\times\frac{L_L}{L_S+L_L}\cdot\frac{E}{N}\qquad(4)$$

where L_L is the inductance of the connecting apparatus, and the period of oscillation is

$$T=\frac{2\pi}{\omega}=2\pi\sqrt{C_C\frac{L_SL_L}{L_S+L_L}}$$

In the investigation, L_L varies from approximately 25 to 325 per cent of the short-circuit inductance of the transformer (L_S), and therefore these values comprehend a wide range of inductances of the connecting apparatus (transformers, rotating machines, and so forth). In Table IV are compared the calculated and experimental data of the transferred surges appearing at the connecting apparatus. The good agreement again confirms the applicability of the simplified methods for practical calculations.

Table IV. Comparison of Calculated Data With Tests—Cable Section and Inductive Apparatus Connected to Secondary of 1,000-Kva Core-Form Transformer

Test Condition	Terminal Inductance (Henry)	Period of Oscillation (Microseconds) Calculated*	Period of Oscillation (Microseconds) Experimental	Crest Voltage e_L in Per Cent e_H Crest Calculated*	Crest Voltage e_L in Per Cent e_H Crest Experimental
Figure 2I	L_L=0.000354	7.2	8.	1.2	1.1
Figure 2I	L_L=0.000894	10.	11.	2.4	2.6
Figure 2I	L_L=0.001727	12.	12.5	3.4	3.4
Figure 2I	L_L=0.004400	14.	14.	4.6	4.1

* Calculated data based on equation 4, and circuit constants in Table I, Figure 2I, and column 2 above. Crest voltage was determined from equation 4 simplified to $(2E/N)\cdot(L_L/L+L_L)$.

Application Problems

The problems encountered in practice are of a varied character, but in general they can be reduced to one of the several conditions in Figure 2 or to modifications and combinations of these. The electrostatic component need be considered only when terminals are left open or connected to an overhead line or other high-impedance circuits. The method is outlined in a previous section. In many of the cases examined, the electromagnetic component alone required attention. The transferred surge then is conducive to calculation according to the foregoing equations 2, 3, and 4, or similar derivations from the equivalent simplified circuit applicable to the problem (Figure 15). The crest voltage of the transferred surge is related to the turn-ratio relation, e_H/N, by a factor A which depends largely on the terminal conditions, the short-circuit inductance (L_S), and the wave of the surge applied (e_H). Typical values of the short-circuit inductance for power transformers are given in Table V.

Table V. Typical Short-Circuit Inductances (L_S) for Power Transformers Values Referred to Low Voltage

Line Voltages (Kilovolts)	Kva Per Transformer	Per Cent Impedance	L_S in Henrys*
220/13.2	20,000	14.8	0.00342
220/13.2	10,000	12.5	0.00578
154/13.2	10,000	11.7	0.00540
132/13.2	10,000	9.2	0.00425
110/13.2	10,000	8.7	0.00402
110/13.2	5,000	8.1	0.00748
66/13.2	10,000	8.1	0.00374
66/13.2	5,000	7.5	0.00692
66/13.2	2,500	7.0	0.01292
44/13.2	10,000	6.2	0.00286
44/ 6.6	2,500	6.2	0.00286
22/ 6.6	10,000	6.2	0.00072
22/ 6.6	2,500	6.2	0.00286

* These values correspond to 60-cycle ratings and for the low-voltage connected delta in three-phase bank. For wye connection reduce values to one third.

In practice, transformers usually are installed in three-phase banks, a condition which results in a transferred surge of equal or lower amplitude than for the single-phase connection previously discussed. For a surge e_H on one phase or simultaneous surges on two phases, the transferred voltage to ground is related to the single-phase transformer connection by the conversion factor K_A in Table VI for the various three-phase bank connections. Correspondingly, the duration of the front of the transferred surge is affected by the factor K_F indicated in the table. For simultaneous surges on all three phases, the electromagnetic component disappears (except for the wye–wye bank connection with both neutrals grounded). Thus the crest voltage of the transferred surge is given by the expression

$$e_L\,(\text{electromagnetic})=\frac{e_H}{N}\cdot A\cdot K_A\qquad(5)$$

A typical example will serve to illustrate the application of equation 5. A wood-pole line connects to a 115,000/13,200-volt, grounded-neutral wye–delta transformer bank. The secondary supplies, through a 125-foot cable, an industrial network which consists of a number of transformer units each feeding individual loads. On a single-phase basis, the circuit corresponds in effect to Figure 15F. In this case, the effective load inductance L_L and the short-circuit inductance L_S are of the same order so that the amplitude factor A in equation 4 is close to unity. The conversion factor for the three-phase bank is $K_A=0.33$. The advantage of arrester protection on the high side in limiting the transferred voltage is apparent. In the event of a severe lightning surge on the wood-pole line, a full-rated high-voltage arrester limits e_H to 450 kv or to a proportionally lower value, provided an 80 per cent arrester is permissible. Then from equation 5 the transferred voltage is (450/5)1x0.33=30 kv. In the absence of arrester protection on

the high side, and should the industrial load become disconnected, the transferred surge across the secondary terminals and the cable may well reach 100 kv.

Similar problems arise in connection with rotating machines. These often supply transformer banks which connect on the high side to overhead lines exposed to lightning. The protection of rotating machines against surges requires that the voltage to ground at the machine should be limited to approximately the low-frequency test voltage of the machine, and, in order to limit the voltage between turns, the front of the surge appearing at the machine should as a rule exceed ten microseconds. The protection of machines is discussed in detail elsewhere.[8,9] The methods presented here are helpful in determining the transferred surge that may appear at the machine. In general, the transformer bank in conjunction with lightning arresters on the high side, as well as cable sections in the low-voltage circuit, usually provides substantial protection to machines. However, each case must be examined individually and on its merits. In important installations or for machines of older design located in vital zones, the addition of special protection at the machines may be justified.

In the problems on transferred surges that have been examined: in some, no protection was found necessary on the nonexposed circuit; in others, adequate protection on the exposed circuit also would provide protection to the nonexposed circuit; in some instances, the addition of protection on the nonexposed circuit appeared desirable. An analysis of the problem in each case provided the basis for sound engineering recommendations.

Appendix

Referring to Figure 15A where rectangular surge e_1 is applied to the primary winding over a transmission line and the secondary connects to a line, the transferred surge e_2 is derived from the circuit equations (expressed operationally)

$$e_1 = L_1 p i_1 + Z_1 i_1 - M p i_2$$
$$0 = L_2 p i_2 + Z_2 i_2 - M p i_1$$

The solution is

$$e_2 = \frac{2MZ_2}{\sqrt{(L_1Z_2 - L_2Z_1)^2 + 4Z_1Z_2M^2}} \times \epsilon^{-\frac{L_1Z_2 + L_2Z_1}{2(L_1L_2 - M^2)}t} \times \sinh \frac{\sqrt{(L_1Z_2 - L_2Z_1)^2 + 4Z_1Z_2M^2}}{2(L_1L_2 - M^2)} t \cdot e_1 \qquad (6)$$

In the usual case $L_1 \gg M \gg L_2$, so that, in effect, equation 6 reduces to

$$e_2 = \frac{M}{L_1}\left[1 - \epsilon^{-\frac{L_1Z_2}{L_1L_2 - M^2}t}\right] e_1 \qquad (7)$$

Table VI. Factors Applied to e_L for Conversion From Single-Phase to Three-Phase Connections*

Bank connections high-voltage to low-voltage winding	Grounded wye to grounded wye; Delta to grounded wye; Delta to wye	Grounded wye to wye; Wye to grounded wye; Wye to wye	Delta to delta	Wye to delta; Grounded wye to delta
K_A (amplitude)	1.00	0.67	0.67	0.33
K_F (front length)	1.00	1.00	0.33	0.33

* For derivation, see paper by Palueff and Hagenguth, reference 1; also see appendix of this paper. The conversion factors correspond here to turn ratio. Application is discussed in equation 5.

That is, in the usual practical problem Z_1 is of secondary or no importance because of the relatively high value of L_1. Circuit A simplifies to B or its equivalent C which, in terms of the turn ratio (N) and short-circuit inductance of the transformer (L_S), becomes circuit D. For a steep-front, exponential-tail surge e_H, the solution of e_L is equation 2.

Should the secondary in Figure 15A connect to a cable section of capacitance C_2, from the circuit

$$e_2 = \frac{M}{(L_1L_2 - M^2)C_2} \times \frac{p}{p^3 + \frac{L_2Z_1}{L_1L_2 - M^2}p^2 + \frac{L_1}{(L_1L_2 - M^2)C_2}p + \frac{Z_1}{(L_1L_2 - M^2)C_2}} \cdot e_1$$

$$e_2 = \frac{M}{(L_1L_2 - M^2)C_2} \cdot \frac{p}{(p+\lambda)[(p+\beta)^2 + \omega^2]} \cdot e_1$$

and the solution is

$$e_2 = \frac{\frac{M}{(L_1L_2 - M^2)C_2}}{(\lambda - \beta)^2 + \omega^2} \times [\epsilon^{-\lambda t} + (\lambda - \beta)\epsilon^{-\beta t} \sin \omega t - \epsilon^{-\beta t} \cos \omega t] e_1 \qquad (8)$$

For the common case $L_1 \gg M \gg L_2$, the circuit simplifies to Figure 15E. For a steep-front exponential-tail surge e_H, e_L is given by equation 3. Similarly, for a cable section and inductive apparatus, we have Figure 15F and equation 4. The transferred surge e_L for other terminal conditions can be derived in like manner.

For three-phase bank connections the solution of the transferred surge is the same form as for the single-phase, except that the conversion factors K_A and K_F appear, respectively, in the amplitude and time constant of the equations. Consider for instance the wye-wye connection (see Table VI), a rectangular surge e_1 applied to one terminal, and for the practical case considered here neglect Z_1. Current i_1 flows in the phase to which e_1 is applied and $i_1/2$ in the other two phases, all in the same direction. In the corresponding secondary windings the currents are i_2, $i_2/2$ and $i_2/2$. We then have the circuit equations

$$e_1 = \frac{3}{2}L_1 p i_1 - \frac{3}{2}M p i_2$$

$$0 = \frac{3}{2}L_2 p i_2 - \frac{3}{2}M p i_1 + \frac{3}{2}Z_2 i_2$$

The maximum voltage to ground is

$$e_2 = i_2 Z_2 = \frac{2}{3} \cdot \frac{M}{L_1}\left[1 - \epsilon^{-\frac{L_1Z_2}{L_1L_2 - M^2}t}\right] e_1 \qquad (9)$$

Thus for this connection $K_A = 2/3$ and $K_F = 1$.

For the wye–delta connection the circuit equations are

$$e_1 = 3/2 L_1 p i_1 - 3/2 M p i_2$$
$$0 = L_2 p i_2 - M p i_1 + 3 Z_2 i_2$$

and

$$e_2 = \frac{1}{3} \cdot \frac{M}{L_1}\left[1 - \epsilon^{-\frac{3L_1Z_2}{L_1L_2 - M^2}t}\right] e_1 \qquad (10)$$

so that, $K_A = 1/3$ and $K_F = 1/3$.

The conversion factors for other three-phase bank connections can be derived in a similar manner or by methods described elsewhere.[1] Their values for the various connections are given in Table VI.

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Effect of Wave Form on Let-Go Currents

CHARLES F. DALZIEL
MEMBER AIEE

Synopsis: This paper is a continuation of "Effect of Frequency on Let-Go Currents"[2] and concludes studies made to determine let-go currents. This paper extends the analysis to cover various wave shapes and includes a method of determining reasonably safe currents for men, women, and children, for sine waves, direct current, and complex wave forms containing both a-c and d-c components. As in previous papers, conclusions regarding reasonably safe electric currents are based on the criterion that a safe current is the let-go current for 99½ per cent of a large group of healthy subjects.

ALTHOUGH it has been demonstrated that let-go currents, and hence the electric-shock hazard due to relatively small electric currents, are controlled by the crest value of an a-c wave and not by its root-mean-square value, the significance of this fact has received little attention. Previous papers on the general subject[1,2] have been concerned chiefly with developing a method of analysis and establishing concepts of reasonably safe sine-wave electric currents for various frequencies and for direct current. The procedure disclosed in this paper extends the analysis to permit estimating reasonably safe let-go currents for practically any commercial wave form, from sine-wave alternating current on one hand to pure direct current as the other extreme. The results of the analysis are believed to be important in increasing the safety of certain electronic devices, filters, and communication circuits, to the end that the sustained shock current obtained from parts of the circuit readily accessible to an operator may be held within reasonably safe or known limits. Also, the results may have value in specifying allowable leakage current to the chassis of radiobroadcast receivers; for improved design of high-voltage television power supplies; for defining the permissible magnitude of the components in the sustained output current from capacitor-discharge electric-fence controllers, and other similar apparatus. Although the conclusions have been based largely on experiments made with 60-cycle alternating current and direct current, the results may also be used for cases involving higher frequencies, but with increased factor of safety.

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CHARLES F. DALZIEL is associate professor of electrical engineering, and supervisor of Engineering Science, and Management War Training, University of California, Berkeley, Calif.

Data presented in this paper were obtained under the supervision of Major John B. Lagen, Medical Corps, United States Army, and Eric Ogden, professor of physiology, University of Texas Medical Branch, and consulting physiologist, John Sealy Hospital, Galveston, Tex. At the time this study was made Major Lagen was assistant professor of medicine and pharmacology, University of California Hospital, San Francisco, Calif., and Professor Ogden was associate professor of physiology, University of California Medical School, Berkeley, Calif. Acknowledgments are also given the men and women who volunteered for these experiments without whose interest and co-operation this work would have been impossible.

Results

Mean let-go current values obtained from tests made with various wave shapes follow the same curve if the crest value of the a-c component is plotted on one axis versus the d-c component on the other. Two conditions must be met in order to have the experimental data fall on the same curve. These conditions are:

1. The reference axis for measurement of the alternating component must be the average value or the direct component.

2. The peak or crest value of the alternating component must be measured in the direction of the maximum total current.

These requirements are illustrated in Figure 1, which gives sketches of the wave forms used in these experiments.

When measurements were made according to these two conditions, points representing the mean let-go current obtained from groups of subjects tested on sine-wave alternating current, direct current, and the four complex wave forms of Figure 1 fell closely on the top curve of Figure 2. The fact that the points fell on the same curve is important, because it permits estimating let-go currents for other wave forms as well as those used to locate the curve. Thus, from two simple measurements one may determine the probable shock hazard for electric currents of any assumed wave form. The 99.5 percentile curves for men and women were computed from equations 3 and 4, reference 2. In the absence of experimental data, the reasonably safe current for children was taken as 50 per cent of the safe values for men. Percentile curves for either more conservative or more liberal probabilities may be computed readily by using a similar procedure.

In these experiments sine waves were superimposed on pure direct current to give the wave forms illustrated in Figures 1B and 1C. In these tests crest A was equal to crest B, and the requirement that the peak of the alternating component be measured in the forward direction, or in the direction of the maximum total current, was unnecessary. The current wave of Figure 1B was similar but not identical to that obtained from the filtered output of an ordinary rectifier. Measurements were made to determine the alternating component of two rectifiers using a shunt and a crest vacuum-tube voltmeter calibrated to read milliamperes. Considerable difference in the magnitude of the opposite crests was obtained, which emphasized the importance of selecting the proper crest of the a-c ripple when using the method for practical problems.

The importance of determining the crest of the alternating component in the direction of the maximum total current is further illustrated from consideration of the wave forms of Figures 1D and 1E. These current waves represent the output from a half-wave and a full-wave rectifier, respectively. Consider crest B for either case. The vacuum-tube crest milliammeter indicated a definite reading when connected to measure crest B, even though the total current was zero. This

Table I. Effect of Wave Form on 180-Cycle Let-Go Current

Subject No.	Triangular Wave: Crest Milliamperes	Triangular Wave: Rms Milliamperes	Sine Wave: Crest Milliamperes	Sine Wave: Rms Milliamperes
2	22.1	12.8	21.4	15.1
12	22.6	13.0	21.9	15.5
21	30.0	17.3	42.4	30.0
22	38.7	22.3	36.3	25.7
23	25.2	14.5	23.2	16.4
27	21.2	12.2	20.1	14.2
30	37.1	21.4	31.7	22.4
31	25.5	14.7	25.2	17.8
32	26.0	15.0	23.3	16.5
36	26.0	15.0	25.7	18.2
37	34.4	19.8	32.5	23.0
40	27.2	15.7	25.5	18.0
45	26.8	15.5	23.5	16.6
54	27.7	16.0	32.8	23.2
55	30.3	17.5	26.0	18.4
61	27.4	15.8	29.4	20.8
64	30.0	17.3	30.0	21.2
67	24.0	13.8	22.9	16.2
74	24.1	13.8	23.8	16.8
Mean	27.7	16.0	27.2	19.3

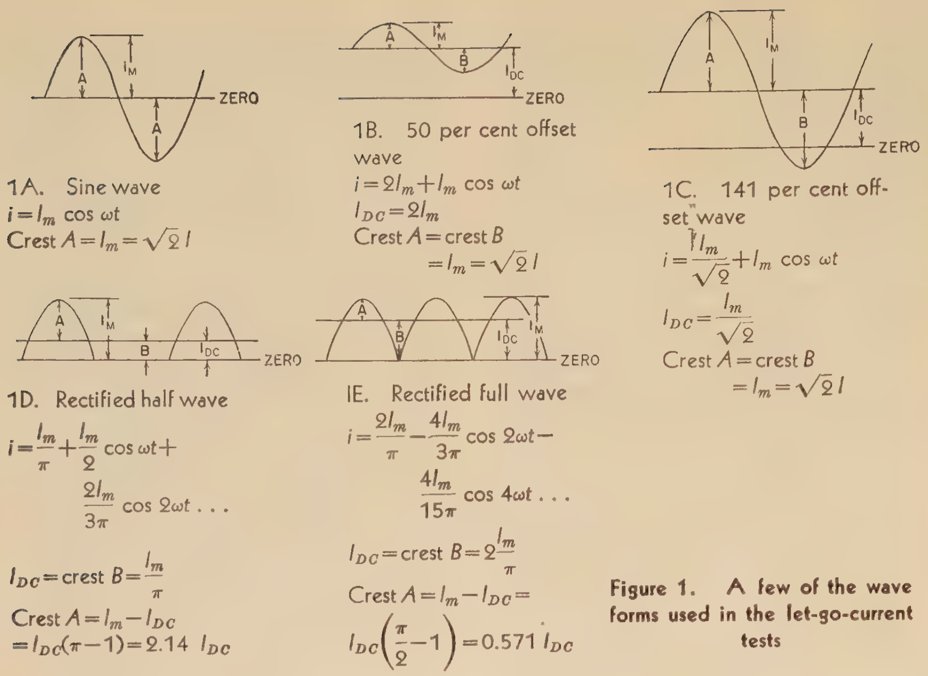


Figure 1. A few of the wave forms used in the let-go-current tests

was due to the fact that the alternating component and the direct component were equal and opposite for the instant considered. It would be surprising indeed if let-go currents were controlled by crest B. As far as determined, there are only two important factors which control let-go currents. These are crest A of the alternating component and I_{DC}, the average value, or the direct component of the wave. The required measurements may be made easily with direct reading instruments. The direct component may be measured with an ordinary d-c milliammeter, and the alternating component may be measured with a half-wave crest vacuum-tube instrument in conjunction with a cathode-ray oscilloscope. The latter is used to permit selecting crest A instead of crest B. Of course, the values may be obtained from a calibrated oscillogram, but this procedure is cumbersome and requires photographic processes.

Although the curves of Figure 2 were determined from six series of tests using 60 cycles and direct current, it is believed that conclusions based thereon will be conservative for wave forms containing higher harmonics or other wave forms likely to be met in practice. Let-go currents for sine waves have a broad minimum from about 15 cycles to 100 cycles. The reasonably safe current curves of Figure 3 indicate that human tolerance increases slowly at first and then quite rapidly for frequencies below or above this range. Predictions made from the curves of Figure 2 for alternating components having frequencies outside the middle band in reality assume that the let-go-current curves are straight lines tangent to the curves of Figure 3 at their lowest points. The predictions may therefore be conservative and well on the side of safety.

The relative-discomfort curve of Figure 3 was obtained by plotting the reciprocals of the average let-go current values for each frequency, that is, the reciprocal of the corrected mean currents, Table I, reference 2. The vertical scale was arranged so that the discomfort would be 100 per cent for 60 cycles. The curve indicates the relative discomfort caused by a given current for the frequency range included in the graph. Although a subject's let-go current increases considerably at the very low frequencies, his muscles follow the current variations, and the sensations, presumably caused by the peaks of the current wave, are more painful than those experienced on the 60-cycle tests. At very high frequencies, sensations of heat rather than pain predominate. The term "relative discomfort" must therefore be given a broad interpretation, and, in the absence of a means of specifying varying degrees of pain, the curve may be taken to show in a general way the discomfort or the relative danger of a given current as a function of frequency.

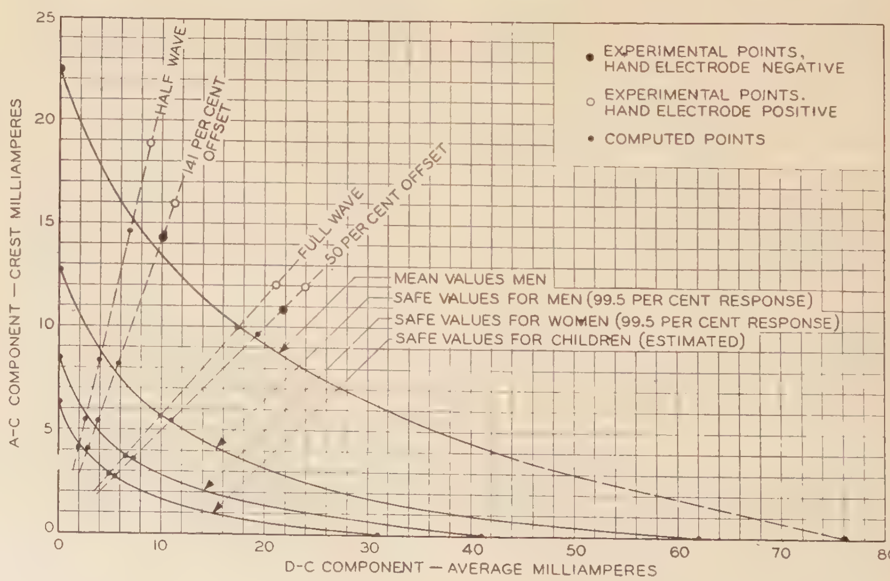
Experimental Data and Analysis

Effects of A-C Wave Form on Let-Go Currents

Before investigating the reactions produced by composite currents, it was considered desirable to make an additional investigation of the effect of wave form on let-go currents for complex alternating currents, that is, for wave forms containing no direct component. As previously reported, let-go currents appeared to be controlled by the crest and not by the root-mean-square (rms) value of the current. In these tests, comparisons were made for 60-cycle sine-wave current alone and in combination with a third harmonic equal to approximately $37^1/_2$ per cent of the fundamental. In one case, the third harmonic was phased so that the resultant wave was sharply peaked. For the other case, the third harmonic was shifted 180 degrees to produce a relatively flat-topped wave. Inspection of the data obtained from 26 subjects indicated that let-go currents were controlled by the peak of the current wave and not the root-mean-square value.[1]

A second series of tests was conducted subsequently in which let-go currents

Figure 2. Let-go-current curves plotted as a function of a-c and d-c components



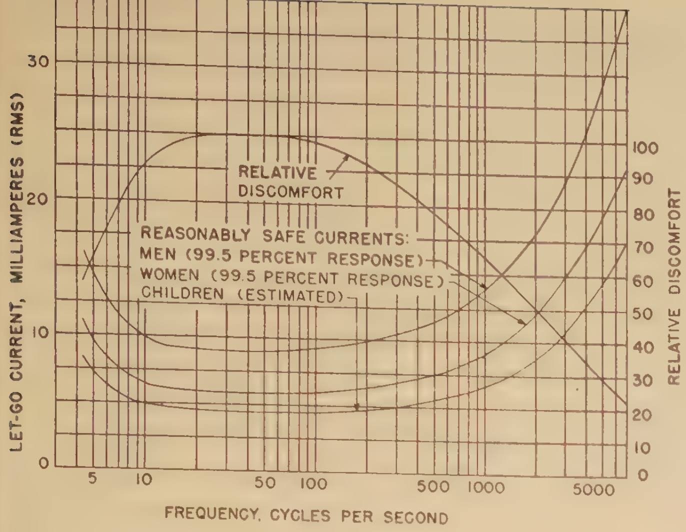


Figure 3. Sine-wave let-go currents and relative discomfort curves versus frequency

for 19 subjects were measured using sine and triangular waves of 180 cycles frequency. An oscillogram of the triangular current wave is shown in Figure 4. The triangular wave was obtained from a rheostat connected across the terminals of the interphase transformer of a small six-phase hot-cathode thyratron rectifier supplying a resistance load. Sine-wave 180-cycle currents were obtained from a beat-frequency oscillator and power amplifier. Experimental data for this test are given in Table I. The number 6 copper-wire electrode wet with salt solution was used for this test and for all other tests discussed in this paper. Although the let-go values were slightly more erratic than those observed for the 60-cycle wave-form tests, comparison of most individuals' values and the mean values for the group corroborate the conclusion that the crest of the a-c wave is the major factor which controls the let-go current.

Reasonably Safe 60-Cycle Sine-Wave Currents for Adults

The following data and analyses were taken from reference 2. The mean sine-wave 60-cycle let-go current for men was 15.87 rms milliamperes = 22.4 crest milliamperes. The 99.5 percentile = 15.87 $(1-0.432) = 9$ rms milliamperes = 12.7 crest milliamperes (from equation 1). The mean sine-wave 60-cycle let-go current for women was 10.5 rms milliamperes = 14.8 crest milliamperes. The 99.5 percentile = 10.5 $(1-0.432) = 5.95$ rms milliamperes = 8.4 crest milliamperes. These values were plotted on the vertical axis of Figure 2 to anchor the curves at this end of the graph.

Reasonably Safe 60-Cycle Sine-Wave Currents for Children

Obtaining let-go currents for young children appears difficult if not impossible of accurate determination. In addition to the reluctance of parents to permit experimentation on their children, limited experience indicates that children are likely to refuse to volunteer for a sufficient number of trials to permit determination of their let-go current with any degree of accuracy. As previously mentioned[2] let-go currents appear related to the muscular development of the wrist and forearm. However, all attempts to find a correlation, other than general appearance, with age, weight, strength of grip, or arm measurements were without conclusive results. In view of the early development of the grip of children it might be expected that their let-go currents would be relatively large; moreover it is a common observation that children appear to be very hardy in their ability to withstand a variety of conditions which would be disastrous to adults. In the interest of establishing a reasonably safe current for normal healthy children, and in the absence of experimental data, it seemed reasonable to estimate the probable safe current for children as 50 per cent of the safe value for normal adult males. Reactions caused by currents of this magnitude were unobjectionable to the men, and they were found to be amply safe for three small boys tested. The following are the 60-cycle sine-wave let-go currents obtained on the boys:

Age 5 years .....Let-go current 7 rms milliamperes*
Age 9 years 3 months..Let-go current 7.6 rms milliamperes
Age 10 years 11 months..Let-go current 9 rms milliamperes

Accordingly, the reasonably safe sine-wave value for normal children was com-

* Determined by Royce E. Johnson, director, electrical standards laboratory, University of Wisconsin, Madison, Wis.

Table II. Let-Go Currents—Sine-Wave and 141 Per Cent Offset-Wave Tests

Hand Electrode Positive			Hand Electrode Negative		
Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)	Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)
73	9.2	7.7	73	9.2	7.5
104	12.6	10.0	104	12.6	8.0
128	13.2	10.0	123	17.8	8.8
93	15.9	11.2	93	15.9	9.3
99	15.8	11.6	99	15.8	9.4
130	16.9	12.2	130	16.9	10.4
129	16.5	12.7	124	21.5	10.5
134	15.1	13.0	132	19.4	11.5
132	19.4	13.0	128	13.2	11.6
			129	16.5	12.0
			134	15.1	12.3
Mean	14.96	11.27		15.81	10.12

	Hand Electrode Positive	Hand Electrode Negative
Corrected mean:		
D-c average and a-c rms	$11.27 \times 15.87/14.96 = 11.96$ milliamperes	$10.12 \times 15.87/15.81 = 10.16$ milliamperes (equation 2, reference 2)
A-c crest	$\sqrt{2} \times 11.96 = 16.91$ milliamperes	$\sqrt{2} \times 10.16 = 14.37$ milliamperes (see Figure 1C)
$99^1/_2$ percentile:		
A-c crest men	$16.91(1.0-0.432) = 9.60$ milliamperes	$14.37(1.0-0.432) = 8.16$ milliamperes
D-c average men	$11.96(1.0-0.432) = 6.79$ milliamperes	$10.16(1.0-0.432) = 5.77$ milliamperes (equation 3, reference 2)
A-c crest children	$0.5 \times 9.60 = 4.8$ milliamperes	$0.5 \times 8.16 = 4.1$ milliamperes
D-c average children	$0.5 \times 6.79 = 3.4$ milliamperes	$0.5 \times 5.77 = 2.9$ milliamperes
A-c crest women	$9.60 \times 10.5/15.79 = 6.4$ milliamperes	$8.16 \times 10.5/15.87 = 5.4$ milliamperes
D-c average women	$6.79 \times 10.5/15.87 = 4.5$ milliamperes	$5.77 \times 10.5/15.87 = 3.8$ milliamperes (equation 4, reference 2)
Polarity effect	$10.16/11.96 = 0.85$	

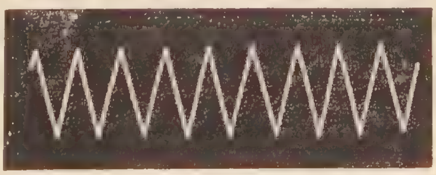


Figure 4. Triangular current wave used to determine effect of wave form on let-go currents for complex a-c wave forms

puted as 0.50×9=4.5 rms milliamperes=6.3 crest milliamperes.

Reasonably Safe Direct Currents

The following d-c release currents were taken from reference 2:

Men, corrected average=76.1 d-c milliamperes
99.5 percentile=62.0 d-c milliamperes
Women, 99.5 percentile=41.0 d-c milliamperes

Using the reasoning of the preceding paragraph, the reasonably safe direct current for healthy children was computed as 0.50×62.0=31.0 d-c milliamperes. These values were plotted on the horizontal axis of Figure 2 to fix the lower limits of the curves. At the conclusion of the a-c let-go-current tests, the two boys used in this investigation released the following direct currents easily and without comment. Higher values were not attempted:

Age 9 years 3 months. . .15 d-c milliamperes
Age 10 years 11 months. . . . . .26 d-c milliamperes

141 Per Cent Offset-Wave-Form Let-Go-Current Tests

In these tests 60-cycle sine-wave alternating current was superimposed on direct current to produce the current wave forms shown in Figure 1C. The proper proportion of the alternating current to direct current was maintained by connecting an ordinary d-c milliammeter in series with a shunt to which was connected a General Radio type 726*A*

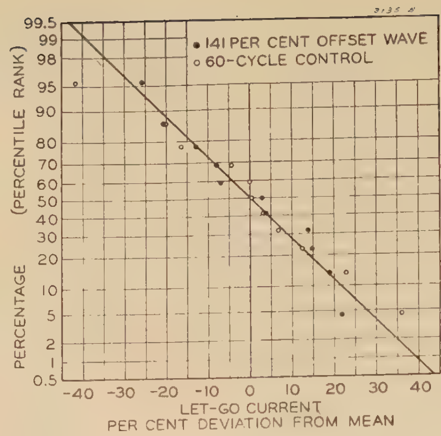


Figure 5. 141 per cent offset-wave let-go-current deviation curve

Hand electrode negative

vacuum-tube voltmeter. Adjustments were made so that the d-c milliamperes were always equal to the root-mean-square a-c milliamperes, resulting in a wave having a ripple equal to

$$\text{Ripple} = \frac{\text{crest a-c milliamperes}}{\text{average milliamperes}} = \frac{\sqrt{2}\ \text{rms milliamperes}}{\text{d-c milliamperes}} = 1.41$$

Experimental values and analysis are given in Table II, and the corresponding deviation curves are shown in Figures 5 and 6. Three determinations of let-go currents were made; one test with the wire electrode negative, one with the wire electrode positive, and one test with commercial 60-cycle sine wave to serve as a control. The three tests were conducted on the same afternoon to obtain comparable results. The sequence of the tests was alternated among the subjects to randomize the effects of fatigue. It will be noted that the experimental points fell closely about the 60-cycle standard-deviation curve. This was interpreted to mean that a sufficient number of subjects had been used to permit predictions of acceptable accuracy to be made for a large group of subjects. The corrected mean of the sample was computed from equation 2, reference 2. Equations 3 and 4, reference 2, were then used to predict reasonably safe currents for men and women. The reasonably safe current for children was taken as 50 per cent of the safe value for men, using the data obtained with the wire electrode negative to give conservative results. Experimental and computed points for the wire negative were then plotted on Figure 2 to assist in locating the mean and safe let-go current curves.

50 Per Cent Offset-Wave-Form Let-Go-Current Tests

A similar procedure was used to determine the let-go currents for a wave having 50 per cent a-c ripple. For these tests the proportion of alternating current to direct current was maintained so that

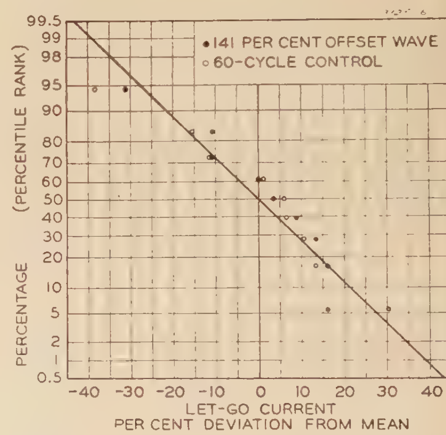


Figure 6. 141 per cent offset-wave let-go-current deviation curve

Hand electrode positive

the d-c milliammeter read exactly twice the crest a-c milliamperes. Experimental values and analyses are given in Table III and the corresponding deviation curves in Figures 7 and 8.

Although the experimental points (especially the 60-cycle control points) did not follow the 60-cycle standard-deviation curve as closely as desired, this was the best that could be done with the available data. At the beginning of the investigation some six years ago, it was planned to obtain values for at least 25 subjects for each test in order to eliminate possible criticism due to deriving conclusions from

Table III. Let-Go Currents—Sine-Wave and 50 Per Cent Offset-Wave Tests

Hand Electrode Positive			Hand Electrode Negative		
Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)	Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave A-C Component (Rms Milliamperes)
123	17.5	7.2	93	17.2	6.0
125	19.3	7.7	123	17.5	6.3
93	17.2	7.9	132	18.3	6.8
82	16.2	8.3	125	19.3	7.1
129	15.1	8.4	129	15.1	7.8
132	18.3	8.6	130	17.6	7.8
135	16.7	8.6	82	16.2	8.1
124	21.5	9.2	124	21.5	8.3
130	17.6	9.3	135	16.7	8.6
134	19.8	9.5	134	19.8	9.8
Mean:	17.92	8.47		17.92	7.66

	Hand Electrode Positive	Hand Electrode Negative
Corrected mean:		
A-c crest	$\sqrt{2}\times 8.47\times 15.87/17.92=10.61$ milliamperes	$\sqrt{2}\times 7.66\times 15.87/17.92=9.60$ milliamperes (equation 2, reference 2)
D-c average	$2\times 10.61=21.22$ milliamperes	$2\times 9.60=19.20$ milliamperes (see Figure 1B)
$99^1/_2$ percentile:		
A-c crest men	$10.61(1-0.432)=6.02$ milliamperes	$9.60(1-0.432)=5.45$ milliamperes
D-c average men	$21.22(1-0.432)=12.05$ milliamperes	$19.20(1-0.432)=10.92$ milliamperes (equation 3, reference 2)
A-c crest children	$0.5\times 6.02=3.0$ milliamperes	$0.5\times 5.45=2.7$ milliamperes
D-c average children	$0.5\times 12.05=6.0$ milliamperes	$0.5\times 10.92=5.5$ milliamperes
A-c crest women	$6.02\times 10.5/15.87=4.0$ milliamperes	$5.45\times 10.5/15.87=3.6$ milliamperes
D-c average women	$12.05\times 10.5/15.87=8.0$ milliamperes	$10.92\times 10.5/15.87=7.2$ milliamperes (equation 4, reference 2)
Polarity effect	$9.60/10.61=0.90$	

Table IV. Let-Go Currents—Sine-Wave and Six Per Cent Offset-Wave Tests

Hand Electrode Negative

Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Offset-Wave Components: A-C Rms Milliamperes	Offset-Wave Components: D-C Average Milliamperes	Remarks
45	16.2	2.3	53	Let-go current
134	15.1	2.9	70	Limit of endurance
140	15.8	3.2	73	Limit of endurance
Mean:	15.7	2.8	65.3	

. A-c crest $= 2.8\sqrt{2} = 3.96$

Per cent ripple $= \frac{3.96}{65.3} = 0.06$

insufficient data. Conclusions based on available data are therefore submitted, subject to slight adjustments, should this be found desirable when conditions permit resumption of the project. Experimental research was stopped when the nation became involved in the war, and it will not be resumed for the duration.

Six Per Cent Offset-Wave-Form Let-Go-Current Tests

The results of let-go current tests made with a composite wave having a ratio of crest alternating current to direct current equal to 0.06 are given in Table IV. This was the last test made before the experiments were terminated, and only three subjects were used. Three values are too few in number to warrant drawing conclusions, and the point representing this test was omitted from Figure 2.

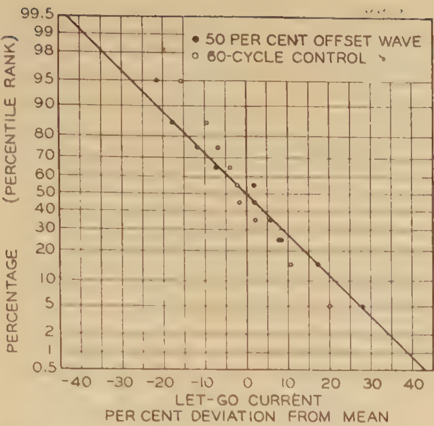


Figure 7. 50 per cent offset-wave let-go-current deviation curve

Hand electrode negative

However, if the point representing the average for the group is inserted on Figure 2, it will be found to be well above the mean curve. This is consistent with the conjecture that the lower end of the curve, which was determined from tests using pure direct current, is well on the side of safety. These data are instructive, since one of the three subjects reached his let-go limit while the other two did not. Although two subjects could still release the electrode, they refused higher currents because of the severity of the shock at the instant of releasing the electrode. This is the only case where let-go currents and release currents were encountered in the same test. These observations stress the importance of considering the alternating component, regardless of how small

Table V. Let-Go Currents—Sine-Wave and Rectified-Half-Wave Tests

Hand Electrode Positive

Subject No.	Sine-Wave Control 60-Cycle (Rms Milliamperes)	Rectified Half-Wave (Average Milliamperes)
45	18.5	6.7
77	14.4	7.0
126	17.8	8.5
129	16.9	8.6
130	18.5	8.9
125	17.3	9.1
132	15.6	9.1
124	17.3	9.5
37	20.9	9.8
21	25.1	10.0
93	17.1	10.1
Mean:	18.13	8.84

Determination of polarity correction factor

From Table II	0.85
From Table III	0.90
Mean	0.875

Corrected mean:

D-c average men	$8.84 \times 15.87/18.13 = 7.74$	milliamperes, hand electrode positive (equation 2, reference 2)
A-c crest men	$2.14 \times 7.74 = 16.57$	milliamperes, hand electrode positive (see Figure 1D)
D-c average men	$0.875 \times 7.74 = 6.77$	milliamperes, hand electrode negative (using polarity correction factor = 0.875)
A-c crest men	$2.14 \times 6.77 = 14.49$	milliamperes, hand electrode negative (see Figure 1D)

99½ percentile:

A-c crest men	$14.49(1-0.432) = 8.22$	milliamperes, hand electrode negative
D-c average men	$6.77(1-0.432) = 3.85$	milliamperes, hand electrode negative (equation 3, reference 2)
A-c crest children	$0.5 \times 8.22 = 4.1$	milliamperes, hand electrode negative
D-c average children	$0.5 \times 3.85 = 1.9$	milliamperes, hand electrode negative
A-c crest women	$8.22 \times 10.5/15.87 = 5.4$	milliamperes, hand electrode negative
D-c average women	$3.85 \times 10.5/15.87 = 2.5$	milliamperes, hand electrode negative (equation 4, reference 2)

Table VI. Sine Wave and Rectified Full-Wave Tests

Hand Electrode Positive

Subject No.	Sine-Wave Control 60 Cycle (Rms Milliamperes)	Rectified Full-Wave (Average Milliamperes)
82	12.2	15.0
128	13.5	16.0
123	16.2	18.5
45	16.2	18.5
131	16.8	19.0
132	16.1	19.8
129	13.5	20.2
122	19.8	21.0
124	18.2	21.5
130	14.6	22.1
125	17.3	23.4
109	20.0	26.4
37	20.9	26.4
105	23.0	27.5
Mean:	17.02	21.09

Corrected mean:

D-c average men	$21.99 \times 15.87/17.02 =$ 19.66 milliamperes, hand electrode positive (equation 2, reference 2)
A-c crest men	$0.571 \times 19.66 =$ 11.22 milliamperes (see Figure 1E)
D-c average men	$0.875 \times 19.66 =$ 17.20 milliamperes, hand electrode negative (using polarity correction factor = 0.875)
A-c crest men	$0.571 \times 17.20 =$ 9.82 milliamperes, hand electrode negative (see Figure 1E)

99½ percentile:

A-c crest men	$9.82(1-0.432) =$ 5.58 milliamperes, hand electrode negative
D-c average men	$17.20(1-0.432) =$ 9.77 milliamperes, hand electrode negative (equation 3, reference 2)
A-c crest children	$0.5 \times 5.58 =$ 2.8 milliamperes, hand electrode negative
D-c average children	$0.5 \times 9.77 =$ 4.9 milliamperes, hand electrode negative
A-c crest women	$5.58 \times 10.5/15.87 =$ 3.7 milliamperes, hand electrode negative
D-c average women	$9.77 \times 10.5/15.87 =$ 6.5 milliamperes, hand electrode negative (equation 4, reference 2)

it may be in comparison to the direct component, when studying shock hazards. Sensations produced by the filtered output of a *B* eliminator, in which the a-c ripple was so small that it was thought negligible, were much more painful than the same current obtained from a battery.

Rectified Full- and Half-Wave Let-Go-Current Tests

Let-go current tests were conducted, using the output of a single-phase mercury-arc rectifier. The results obtained using one anode are given in Table V and Figure 9. Similar data obtained for both anodes are given in Table VI and Figure 10. These tests were conducted with the hand electrode positive. Sixty-cycle sine-wave let-go tests were also conducted to serve as a control for computing the response for a large group, as previously discussed.

It was found that the sensations produced by the current were more painful and the let-go current was less when the hand electrode was negative in comparison to tests made with the hand electrode positive. The tests using the rectified waves were conducted with the hand electrode positive, and it was necessary to determine a correction for polarity, in order to compare the points representing these tests with the preceding data. The effect of polarity was determined from the 141 per cent and the 50 per cent offset-wave tests. A polarity correction factor was taken equal to the ratio (mean let-go current, hand electrode negative) ÷ (mean let-go current, hand electrode positive). The average ratio obtained from these two tests was taken as the polarity correction factor = 0.875. This factor was used in the analysis of the data of Tables V and VI, and the points determined thereby were given due consideration in locating the curves of Figure 2.

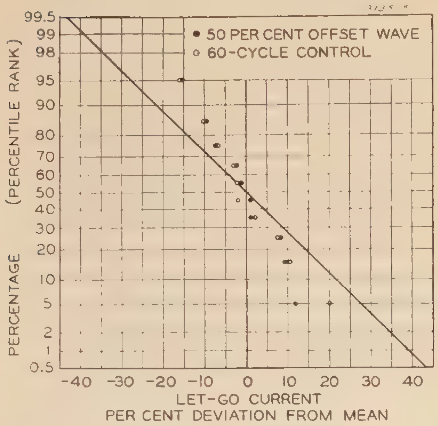


Figure 8. 50 per cent offset-wave let-go-current deviation curve

Hand electrode positive

The polarity effect, which was found to decrease the average let-go currents $12^1/_2$ per cent when the hand electrode was negative, may have value from a safety standpoint. The decrease in both the sensations and the shock hazard for a given current when the hand electrode is positive may be helpful in arriving at a decision whether to ground the positive or the negative side of a circuit.*

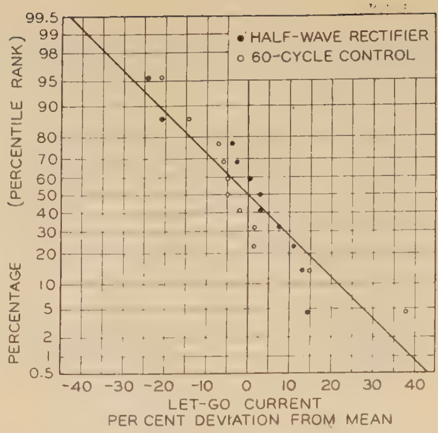


Figure 9. Rectified half-wave let-go-current deviation curve

Hand electrode positive

As can be seen from inspection of Figure 10, the experimental points for the full-wave-rectifier test followed the 60-cycle standard-deviation curve closely. In comparison, the points for the half-wave-rectifier test were somewhat more scattered about the deviation curve (see Figure 9). It should be noted that the scattering about the deviation curve was about the same for both the wave form under investigation and the 60-cycle control. The scattering was explained as due to the limited number of subjects tested rather than to suggesting a different response for rectified half-wave currents. It is believed that data from a larger group of subjects would have reduced the discrepancies and resulted in the points following the 60-cycle deviation curve as closely as that found for the other tests.

Smooth curves were drawn through the computed points (wire negative) to give the curves of Figure 2. Several other attempts were made to analyze the data, but for each case tried at least some of the computed points failed to follow a common curve. Points obtained from the analysis described in the foregoing paragraphs closely follow the same curve, and for this reason it is believed that the procedure is sufficiently general to permit predicting reasonably safe currents with acceptable accuracy for wave shapes similar as well as those actually used in the experiments. Although one must use caution when arriving at conclusions, the good agreement between the experimental points and the theory suggest that the analysis should be valid for predictions involving complex or composite waves containing a-c components and d-c components in the proportions likely to be met on commercial apparatus or associated circuits. As previously discussed, it is believed that errors due to wave forms containing high-frequency components should be on the safe side, and the resulting currents should be on the side of safety.

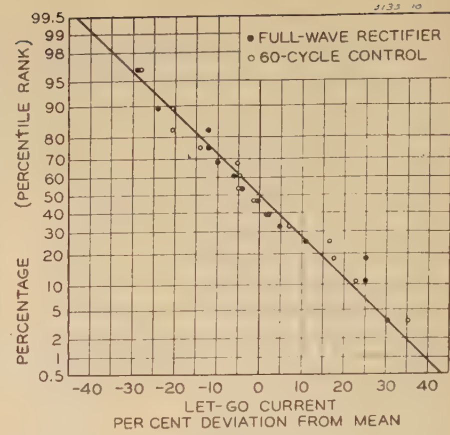


Figure 10. Rectified full-wave let-go-current deviation curve

Hand electrode positive

* It is well known to physiologists that during passage of direct current through a nerve the regions in the neighborhood of the electrodes are altered with respect to

1. Sensitivity to stimulation.

2. Ability to conduct nerve impulses.

The negative region becomes more sensitive and a better conductor.

References

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2. Effect of Frequency on Let-Go Currents, **Charles F. Dalziel, Eric Ogden, Curtis E. Abbott,** AIEE Transactions, volume 62, 1943, December section, pages 745–50.

Effect of Frequency on Let-Go Currents

CHARLES F. DALZIEL
MEMBER AIEE

ERIC OGDEN
NONMEMBER AIEE

CURTIS E. ABBOTT
ENROLLED STUDENT AIEE

Synopsis: This paper on electric shock covers the subject of sine-wave let-go currents for both men and women and contains an analysis which permits improved accuracy in predicting the response for large groups based on experiments made on a relatively small number of subjects. It should be of especial interest to persons who have had accidents in which they barely escaped "freezing" to an electrified conductor and also to those interested in electrical safety. The range of frequencies covered is from 5 to 10,000 cycles and also direct current. The paper is the most comprehensive treatment of the subject yet published as the analysis permits predicting currents of a specified degree of safety for both men and women for this wide frequency range.

THIS paper presents the results of a study to determine the effect of frequency on let-go currents made on 134 men and 28 women at the University of California. Let-go currents are important, because it has been found that an individual can withstand, with no ill aftereffects, repeated exposure to his let-go current for at least the time required for him to release the conductor. Larger currents are dangerous, because release from the circuit during accidental contact is problematical. A reasonably safe current for normal men and women is defined as the let-go current for $99\frac{1}{2}$ per cent of a large group. A knowledge of let-go currents is important for the formation of safety codes, for explaining accidents, and for the design of electrical devices with exposed electrodes.

The majority of the data was presented in a preliminary report before the Institute in 1941.[1] The number of male subjects for the 60-cycle test now totals 134, the data on females are new, and an improved analysis is disclosed. Use of the new analysis permits improved accuracy in predicting results for a large group from tests made on a small but representative sample. The paper includes an example in which the method is used to predict probable let-go currents for all normal men and women for a frequency range from 5 to 5,000 cycles. Estimates are given for 10,000 cycles and also for direct current.

Determination of Let-Go Currents

An individual's let-go current is defined as the maximum current he can tolerate when holding a copper conductor in one hand and yet let go of the conductor by using muscles directly affected by that current. No material changes in experimental procedure, safety precautions, selection of subjects, or equipment were found to be necessary, and, in general, the experiments were continued as described in the preliminary report. The following résumé is included for convenience. The subjects held and then released a test electrode consisting of a number 6 copper wire, and the circuit was completed by placing the other hand or foot on a flat brass plate, or by clamping a conducting band lined with moist cloth firmly on the upper arm. After one or two preliminary trials to accustom the subject to the sensations and muscular contractions produced by the current, the current was increased to a certain value and the subject was commanded to let go of the wire. If he succeeded, the test was repeated at a slightly higher value. If he failed, a lower current was used, and the values were again increased until the subject could no longer release the test electrode. The end point was checked by several trials. The agreement between those trials was within about two to five per cent, and the highest value was taken as the individual's let-go value in order to eliminate the effects of fatigue.

The tests reported here were conducted with hands wet with a common saltwater solution to secure uniform conditions and to reduce the sensation of burning caused by high current densities at tender spots and at the instant of releasing the electrode. Other tests were made with dry hands, hands moist from perspiration, and hands dripping wet from weak acid solutions. The effect of the size of the electrodes was also investigated. As reported previously, the location of the indifferent electrode, the moisture conditions at the points of contact, and the size of the electrodes had no appreciable effect on an individual's let-go current. It is believed that conclusions based on tests using the wet copper electrode may be used to predict reasonably safe electric currents with an accuracy sufficient for most practical purposes.

There was considerable variation in an individual's value on succeeding trials, the trend usually being toward higher values. Therefore the largest current released on the *first* test on any frequency was taken as the let-go current for that frequency. This was done to include the

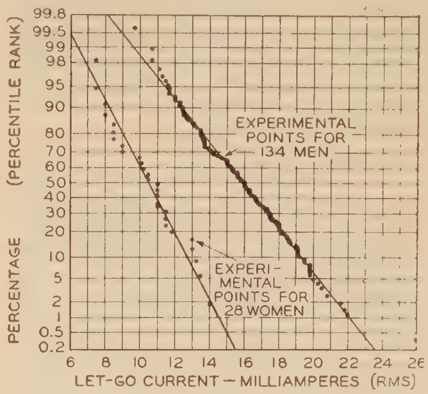

Figure 1. 60-cycle let-go-current distribution curves for men and women

Electric currents of a specified degree of safety for normal men or women may be obtained directly from the curves

element of surprise to as great an extent as possible and to give conservative results. Psychological factors, especially fear and competitive spirit, were the most important causes for the variations. Physiological factors also played an important part, but so far their exact mechanism remains unknown. It seemed that let-go currents in both sexes were related to the muscular development of the wrist and forearm. Husky subjects having low let-go values could almost invariably be persuaded (or heckled by other subjects) to continue the test until their values were in line with others of similar physique. Although attempts were made to quantitate this relationship by correlating let-go currents with the strength of grip and with forearm and wrist diameters, the results were inconclusive. Perhaps this was because of the narrow range and lack of precision involved in the physiological measurements; moreover, forearm and wrist diameters are determined to an uncertain degree by fat and bone as well as by muscular tissue.

Sixty-cycle let-go currents were meas-

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CHARLES F. DALZIEL is associate professor of electrical engineering and supervisor of Engineering, Science, and Management War Training, University of California, Berkeley, Calif. ERIC OGDEN, now professor of physiology, University of Texas Medical Branch, and consulting physiologist, John Sealy Hospital, Galveston, Tex., was associate professor of physiology, University of California Medical School, Berkeley, Calif., at the time this study was made. CURTIS E. ABBOTT, now first lieutenant in the Signal Corps, United States Army, was a student in electrical engineering, University of California, Berkeley, Calif., at the time this study was made.

ured on 27 additional women, bringing the total to 28. The subjects ranged in age from the late teens to the early twenties, were light in stature, and obviously not accustomed to hard physical work, and their forearm muscles were not particularly well developed. Although the women volunteered freely for the tests, it proved impossible to develop enthusiasm or any degree of competition at high currents. The results are probably representative for the sedentary type; however, from observation of the reactions of subjects having the greatest muscular development, it is likely that the values were considerably lower than those which would have been obtained, had a typical group of mature healthy women used to physical labor been used. Results based on these data therefore should be conservative and on the side of safety.

A Safe Electric Current

The principle of biological variability is recognized so universally that no attempt should be made to specify any electric current as safe for all people. The press contains frequent accounts of fatalities ascribed to heart failure caused by overexcitement, intense emotion, or shock (shock of injury, *not* electric shock). Some of the subjects volunteering for these tests became frightened and trembled all over; some even complained of pain when holding the test electrode before the current was turned on. Although these persons were not used in the experiments, the experiences dramatically illustrated the possibility that a person with a diseased heart might succumb from any contact, or even the fear of contact, with an electric circuit. This possibility must be recognized, and an occasional death is to be expected as a result of casual contact involving electric currents known to be harmless to the great majority of healthy individuals.

Quite aside from determining an absolutely safe electric current for all human beings is the practical problem of determining a current which would be reasonably safe for most normal healthy adult men and women. In certain cases, economics or special applications may necessitate the use of current values which are hazardous to only a small percentage of a large group. This is particularly important at the present time, because of the widespread use of electric devices having exposed electrodes.[2,3]

Several factors must be considered in deciding what constitutes a reasonably safe electric current. The victim of a severe accidental electric shock can often release himself by using muscles little affected by the current. The muscular reactions produced by the current may tend to break the circuit rather than to improve it, or loss of balance may free the victim. However, it is doubtful if any of these methods would be of avail when contact is established by gripping an electrified conductor so that the path of the current was across the body. The hazard due to electric shock depends upon several factors, the most important being the magnitude of the current, the current pathway through the body, the time of contact, and the physical condition of the victim.

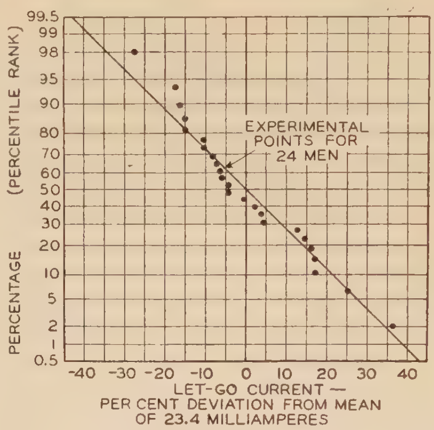

Figure 2. Five-cycle deviation curve for men

Conclusions regarding reasonably safe electric currents were based on the factors mentioned in the preceding paragraphs, together with the results of carefully conducted experiments made on a total of approximately 250 men and 28 women, and involved several thousand individual tests. Without a single exception, it was found that an individual could withstand, with no ill aftereffects, repeated exposure to his let-go current for at least the time required for him to release the conductor. The current pathway for the majority of the tests was between the hands. A few experiments were made with current pathway between one hand and one or both feet; in other tests the pathway was between the hand and an arm band.[2,3]

It is the authors' studied opinion that, for most practical purposes, the maximum electric current which is safe for an individual is the greatest current he can release by using muscles directly affected by that current. From this, it was concluded that a reasonably safe electric current for most normal healthy physically fit adult men and women is the let-go current for $99^1/_2$ per cent of a large group. Numerical determination of reasonably safe currents from the let-go current tests will be discussed later at an appropriate point in the text.

Analysis of Let-Go Currents

Let-go currents were obtained for groups of male subjects ranging in number from 25 to 134 using sine-wave alternating currents from 5 to 10,000 cycles and also direct current. The let-go currents for each test were then plotted on probability cross-section paper, and the straight line governed by the majority of the points was drawn to obtain the normal distribution curve for the sample. An advantage of this method is that points obviously not conforming to the sample may be disregarded by inspection. The procedure is illustrated in Figure 1 which gives the 60-cycle let-go-current distribution curves for 134 men and 28 women. Distribution curves for the other frefrequencies were similar, except for the

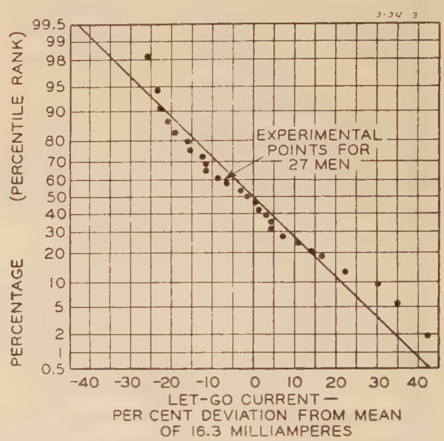

Figure 3. Ten-cycle deviation curve for men

current scale or the slope of the distribution curve.

It was found that, when let-go currents were plotted as per cent deviations from the mean of any test, the slope of all the resulting straight lines was the same. This was true for both men and women on the 60-cycle tests and for other tests from 5 to 5,000 cycles. Deviation curves for the 60-cycle tests are given in Figures 5 and 5A. Since only a small number of subjects was used for other than the 134-men 60-cycle test, the straight line response of Figure 5 was drawn on each curve as the probable response for the other frequencies also. Within the range of 5 to 5,000 cycles, the straight line representing the 60-cycle response was the best line that could be drawn through the majority of the points (see Figures 2 to 10 inclusive). The fact that let-go current values follow the same deviation curve may be illustrated in a rather strik-

ing manner if all the points (excluding the 60-cycle values, to avoid confusion) are plotted on the same curve sheet. The wide discrepancies of one or two points from the deviation curves were due to the relatively small number of subjects used. The reason that let-go currents follow the same curve for this range of frequencies may be related to the similarity of the physiological phenomena, which were characterized by painful muscular contractions. Outside of this frequency range, sensations of heat predominate. Strictly comparable measurements are not possible for direct current.

In previous studies, the mean of a test was taken as the most reliable measure for the sample, and the accuracy of prediction was chiefly dependent upon the number of subjects used for a particular test. Improved accuracy for small samples results from plotting the data as percentage deviations from the mean of the sample and comparing the response with the slope of the 134-men 60-cycle deviation curve of Figure 5. In the five-cycle test, shown in Figure 2, two points deviated from the rest of the data sufficiently to suggest that they should be discarded. Accordingly, the points were recomputed after the data from these two subjects had been rejected, leaving a total of 24 points. Such rejection was not found to be necessary in the other tests.

Predictions for any percentile rank are obtained from

Percentile rank = mean of sample ± deviation from mean × mean of sample

or

Percentile rank = mean of sample (1 ± deviation from mean) (1)

It is evident that the accuracy of prediction depends to a large extent upon the accuracy of the 134-men 60-cycle deviation curve and upon the subjects being a true random sample of the population. The data upon which this curve was con-

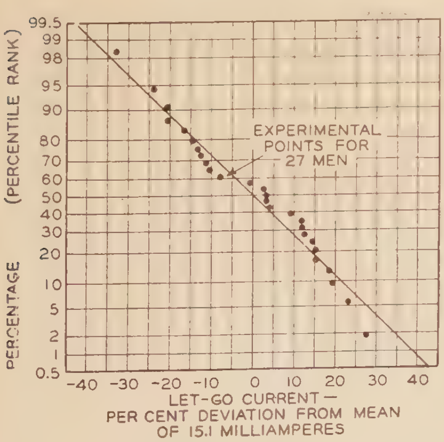

Figure 4. 25-cycle deviation curve for men

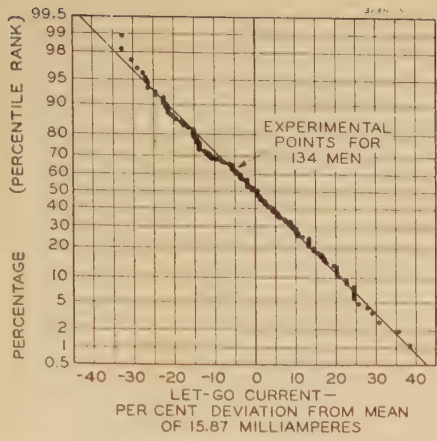

Figure 5. 60-cycle deviation curve for men

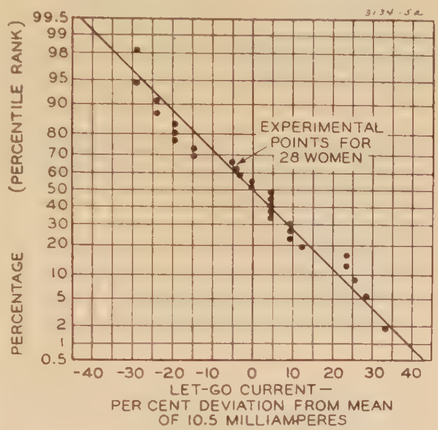

Figure 5A. 60-cycle deviation curve for women

structed were obtained from carefully conducted tests made during a six-year period. Although care was taken to obtain only subjects in good physical condition, they were not selected especially with respect to age, weight, or muscular development. They represented a wide variety of normal men, with an age range of from 21 to 46. The close agreement between the 134 points and resulting straight line indicates that the response actually followed a normal distribution. This is exactly what should have been observed, had an infinite sample been tested. This close agreement is presented to substantiate the conclusion that a sufficient number of subjects has been examined to permit determination of the response of a large sample. Proceeding to the limit, it is assumed that a sufficient number of subjects has been tested to obtain the representative response of all normal men with an accuracy sufficient for engineering purposes.

An important factor controlling the accuracy of predictions for other tests is the necessity of procuring a representative sample. It is entirely possible that any small sample may have a mean considerably above or below that of an average sample, irrespective of agreement with the standard deviation curve. This is particularly important in experiments such as this, because it was usually found that let-go currents on the same frequency had a tendency to increase as the number of tests increased. This was attributed to the subject's becoming accustomed to the ordeal. Let-go-current tests should be conducted for the given test conditions and also on sine-wave 60 cycles to serve as a control. The order of the tests among the subjects should be changed frequently to randomize effects of fatigue.

The corrected mean, that is, the mean for an infinite sample equals

Corrected mean of sample

$$= \frac{\text{Mean 60-cycle let-go current of all men}}{\text{Mean 60-cycle let-go current of sample}} \times \text{mean of sample for given test conditions}$$

or

Corrected mean of sample

$$= \frac{15.87}{\text{60-cycle control}} \times \text{mean of sample} \qquad (2)$$

The percentile rank for an infinite group is obtained from

Percentile rank = corrected mean of sample × (1 ± deviation from mean) (3)

For example, the $1/2$ or $99\,1/2$ per cent response for any frequency between 5 and 5,000 cycles is given by

Deviation from the mean for $1/2$ per cent or $99\,1/2$ per cent = ±0.432 (from Figure 5)

$1/2$ or $99\,1/2$ percentile rank = corrected mean of sample × (1 ± 0.432)

Figure 14 shows the effect of frequency on let-go currents. Significant data are included in Table I to illustrate the method of analysis. At the time these data were obtained, no 60-cycle control was taken, but the subject's mean 60-cycle let-go current was used to give an

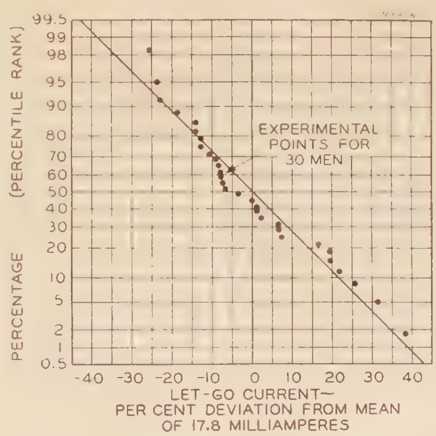

Figure 6. 180-cycle deviation curve for men

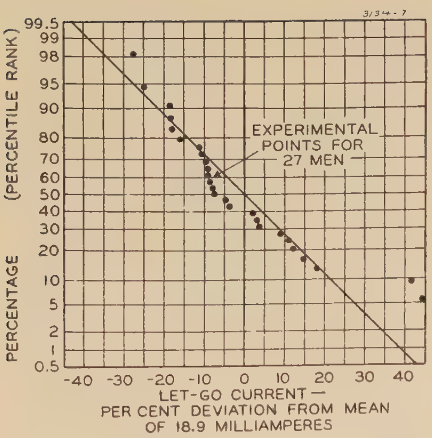

Figure 7. 500-cycle deviation curve for men

approximate correction. Computed points lie close to smooth curves, the data appear to be consistent, and the curves should represent the response for a large group of normal men. The curves read from below upwards illustrate the probability that the current they represent will be dangerous. As previously discussed, a reasonably safe current for normal male adults is proposed as that represented by the lowest curve which indicates values safe for at least 99½ per cent of the group tested.

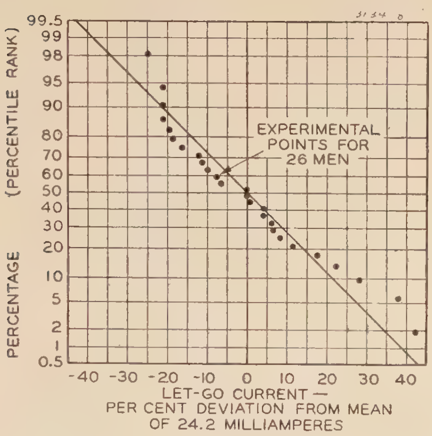

Figure 8. 1,000-cycle deviation curve for men

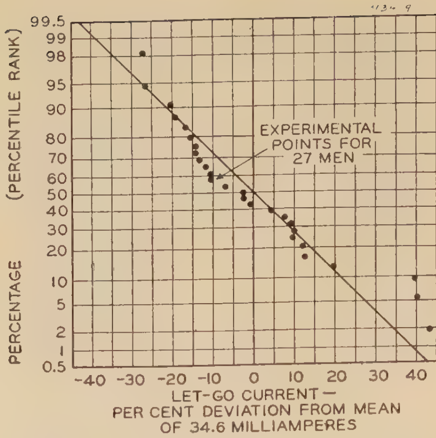

Figure 9. 2,500-cycle deviation curve for men

A safe current for normal women (Figure 5A) was computed as follows:

99½ percentile rank for 60 cycles $= 10.5\ (1-0.432) = 6.0$ milliamperes rms from equation 1

The response for normal women on any frequency or wave form is obtained from

Percentile rank

$$= \frac{10.5}{15.87}\left[\begin{array}{c}\text{corrected mean of sample for}\\ \text{men on desired frequency or}\\ \text{wave form}\end{array}\right] \times (1 \pm \text{deviation from mean}) \quad (4)$$

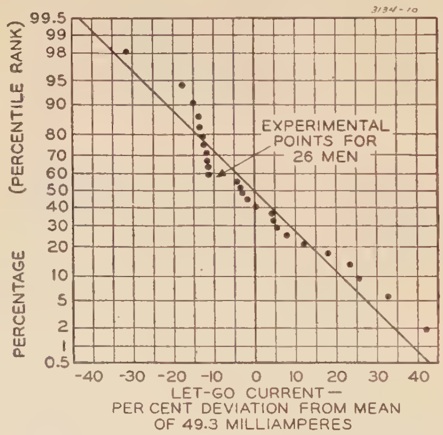

Figure 10. 5,000-cycle deviation curve for men

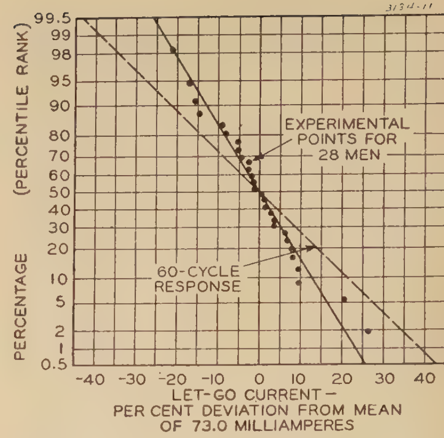

Figure 11. 10,000-cycle deviation curve for men

from which it is apparent that the let-go current for women is equal to 10.5 ÷ 15.87 = 66 per cent of the corresponding percentile values for men. Hence the safe current for normal women is equal to approximately two-thirds of the bottom curve of Figure 14. This procedure is predicated on the assumption that the physiological phenomena associated with let-go currents for both normal men and women are similar and that the ratio of the let-go currents is the same at all fre-

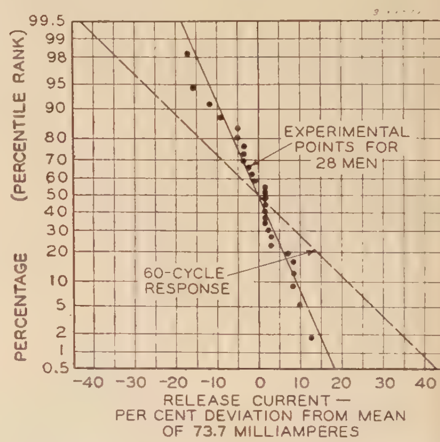

Figure 12. D-c deviation curve for men

Table I. Let-Go Currents for Men Versus Frequency

Frequency	Mean of Sample	Mean 60-Cycle for Group	Corrected Mean	Percentile Rank ½	1	25	50	75	99	99½
Deviation from mean (from Figure 5)				0.432	0.390	0.115	0.00	−0.115	−0.390	−0.432
60	15.87	15.87	15.87	22.7	22.1	17.7	15.87	14.0	9.7	9.0
5	23.4	14.56	25.5	36.5	35.5	28.4	25.5	22.6	15.5	14.5
10	16.27	14.94	17.3	24.8	24.0	19.3	17.3	15.3	10.5	9.8
25	15.14	15.11	15.9	22.7	22.1	17.7	15.9	14.1	9.7	9.1
180	17.8	15.42	18.3	26.2	25.4	20.4	18.3	16.2	11.2	10.4
500	18.9	15.53	19.3	27.6	26.8	21.5	19.3	17.1	11.8	11.0
1,000	24.2	15.84	24.2	34.7	33.7	27.0	24.2	21.5	14.8	13.7
2,500	34.6	15.57	35.2	50.4	48.9	39.3	35.2	31.2	21.5	20.0
5,000	49.3	15.17	51.6	73.9	71.7	57.8	51.6	45.7	31.5	29.3
Deviation from mean (from Figure 11)				0.260	0.234	0.068	0.00	−0.068	−0.234	−0.260
10,000	73.0	15.48	74.8	94.3	92.3	79.9	74.8	69.7	57.3	55.3
Deviation from mean (from Figure 12)				0.185	0.167	0.049	0.00	−0.049	−0.167	−0.185
D-c	73.7	15.37	76.1	90.3	88.9	79.9	76.1	72.5	63.5	62.1

quencies. It is believed that discrepancies due to sex differences are too small to affect practical conclusions with regard to electrical safety.

Sensations at the higher frequencies, especially those on 10,000 cycles or higher, were less painful, and the severity of the muscular contractions was less than that on the lower frequencies. Alarming sensations of internal heating were produced, muscular control was sluggish, and the time to let go the conductor increased considerably. These differences in physiological phenomena were associated with a deviation curve having a greater slope (see Figure 11). A straight line was drawn through the majority of the points to determine the deviation curve. The

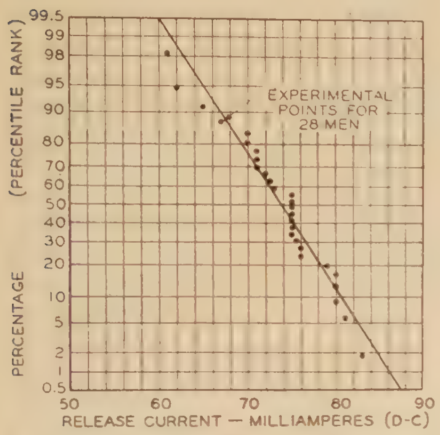

Figure 13. D-c release-current distribution curve for men

60-cycle response was sketched on the curve for comparison. Although the experimental points fell close to the deviation curve, the accuracy of predicting the response for a large group is less than that for the lower frequencies, since it depends upon the accuracy of the small amount of data available on this frequency alone. However, the data were analyzed in a manner similar to that used for the other frequencies and are included in Table I.

Tests on steady or gradually increasing direct currents produced sensations of internal heating rather than muscular contractions. Sudden changes in current magnitudes produced muscular contractions, and interruption of the current produced a very severe shock. The muscular reactions when the test electrode was released at the higher values were objectionable, and sooner or later all subjects declined to take more punishment. Tests were made on 28 men, and in each case little difficulty was experienced in releasing the electrode. The maximum a subject would take and release was termed the release current. It represents the limit of voluntary endurance rather than the let-go limit. The deviation curve for these tests is shown in Figure 12 and the release curve in Figure 13. At the conclusion of the 60-cycle let-go-current tests on 27 women, the experiments were terminated with one or two release tests using direct current. After one or two preliminary trials, the current was increased to a maximum of 35 milliamperes. Each subject released the electrode without complaint or difficulty. As previously reported,[1] a single woman was tested at the time the d-c tests were made on the men. She released 56 milliamperes direct current before refusing more.

The data for the d-c tests were analyzed in the same way as those for alternating currents but with less certain justification:

$$\text{Corrected mean of sample} = \frac{15.87}{15.37} \times 73.7$$
$$= 76.1 \text{ milliamperes direct current}$$
(from equation 2)

Deviation from mean for $99^1/_2$ percentile rank $= -0.185$ (from Figure 12)

$99^1/_2$ percentile rank for men
$= 76.1(1-0.185) = 62.0$ milliamperes direct current (from equation 3)

$99^1/_2$ percentile rank for women
$$= \frac{10.5}{15.87} \times 76.1(1-0.185) = 41.0 \text{ milliamperes}$$
direct current (from equation 4)

Based on the preceding data it is concluded that 62 and 41 milliamperes is a reasonably direct current for men and women respectively. Undoubtedly the maximum direct currents which are reasonably safe are in excess of these values. However, it would be unwise to suggest higher values as safe until additional experimental research is available.

Electrical Safety

Although current and *not* voltage is the proper criterion of shock intensity, the danger of accidental electric shock on commercial circuits is due almost entirely to the voltage of the circuit. Rather than that the layman or the public should be confused with a technical argument, he must be warned that all power circuits are dangerous. Many deaths are caused each year from accidental contacts with ordinary 120-volt lighting circuits. Contacts with house circuits are especially hazardous in moist or wet locations.

Conclusions

1. A reasonably safe electric current for normal healthy adults is the let-go current which $99^1/_2$ per cent of a large group can release by using muscles directly affected by that current.

2. The reasonably safe 60-cycle current for most normal healthy adult men is about nine milliamperes; the reasonably safe 60-cycle current for most normal healthy adult women is about six milliamperes.

3. Let-go currents are affected by fre-

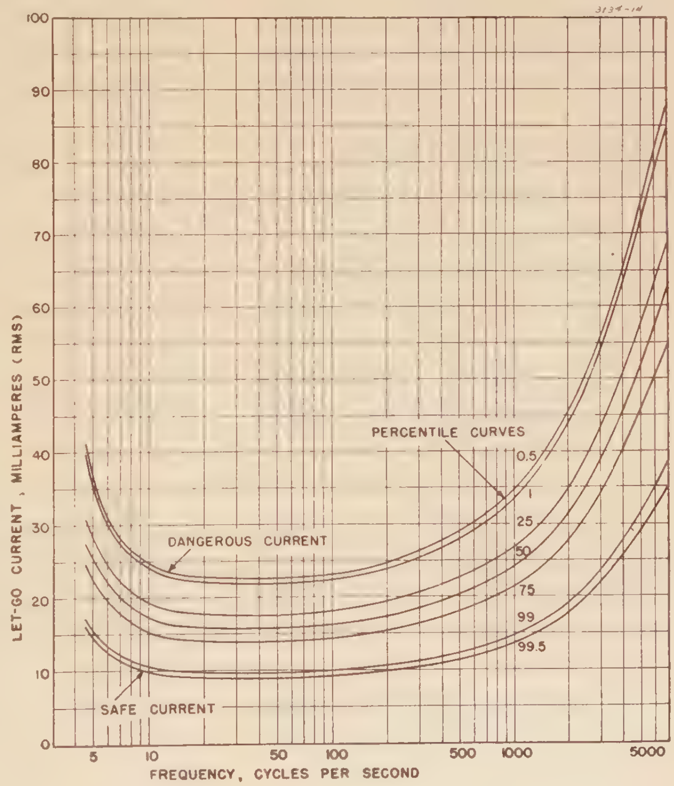

Figure 14. Sine-wave let-go current for men versus frequency

Current values become dangerous progressively to an increasing number of persons, as indicated by the percentile values on the right-hand side of the curves

Values for women are approximately 66 per cent of the current values shown on the curves

Some Measures of Electrical-Brush Disintegration

M. S. COOVER
FELLOW AIEE

E. E. JONES
ASSOCIATE AIEE

Synopsis: So far as can be determined, the development of the concepts of wear resistance, wear resistivity, wear susceptance, and wear susceptibility as given in this paper is thoroughly new and original. These concepts are believed to be significant and useful tools that may further an understanding of the basic problems of sliding electrical contacts. For example, they offer means for making a valid comparison of brushes and brush materials insofar as wearing qualities are concerned. Moreover, they provide a clue to the proper consideration and evaluation of the large number of variables that are at work as brush disintegration takes place.

THE subject of wear itself is beset by many complications arising from the fact that the subject, at best, is largely statistical. It is accepted generally among authorities that there is no such thing as a perfect sliding contact, in which all sliding phenomena are continuous and uniform. Only grossly macroscopic data appear to be continuous, while data approaching microscopic proportions reveal discontinuities that are more or less periodic. It is conceded generally that a sliding contact consists of a number of minute point contacts, whose behavior is extremely individualistic. All of the characteristics of the macroscopic contact originate at these minute points; and it is logical, therefore, to infer that extreme conditions, such as excessive pressures, excessive current densities, excessive temperatures, and others, exist at these points. It is exceedingly difficult, therefore, to obtain a true picture of a sliding contact by means of the macroscopic data that are available to most experimenters and engineers. This paper is intended as an aid in the efforts to study sliding-contact materials by means of these macroscopic data.

Further complications arise when the term *wear* is applied to the undesirable destruction of all surfaces, whatever the manner in which the destruction may occur. The wear of shoe soles and heels, the wear of piston rings and cylinder walls, the wear of bearings, the wear of cutting edges, and many other types of wear, each involves a peculiar and, perhaps, unique combination of forces that provide the mechanism of wear. Therefore, when discussing wear, it is necessary to specify the particular kind that is being considered as well as the multitude of conditions that influence it.

This paper is intended to include only the type of wear that is encountered in the use of continuously sliding contacts, particularly the end wear of brushes riding on collector rings. Even in this simple case, however, it is impossible to comprehend the true mechanism of the wear of a sliding contact in all its details. As evidence of the great complexities that exist in a sliding contact, an abbreviated chart of the known variables that enter into the phenomenon of the wear of electrical brushes is shown in Figure 1. It is beyond the scope of this paper to discuss the part that each variable has in the whole process of brush wear, but it is not difficult to conceive by means of this chart the truth that the whole process is highly involved and cannot be disentangled very easily, if at all.

Comparative

Nevertheless, it is desirable that electrical brushes be compared on the basis of relative wearing qualities as well as on the bases of contact resistance, current-carrying capacity, and other performance characteristics. V. P. Hessler has made a successful attempt within limitations to study the effects of various operating conditions upon electrical-brush wear as well as on brush contact drop.[1–3] He considered chiefly the effects of current density, ring speed, brush pressure, and relative humidity. That material has been distinctly useful in advancing toward a better understanding of basic relationships.

However, further progress could be made if a concept of wearing quality were developed that would apply strictly to the brush and not include the properties of the external system of which the brush is a part. No fair comparison can be made of brushes at the present time without testing all of them on the same test equipment and under identical conditions. Since many different grades of brushes are made for use under widely different conditions, testing them under like conditions is not always useful. Even though they be tested under like conditions, there is yet the problem of distinguishing between the effect of the conditions imposed on the interface by the whole sliding system, including the brush, and that of the structural properties of the contact faces. It is the intended purpose of this paper to develop a means whereby this distinction between the conditions responsible for producing wear and those responsible for resisting wear can be made. The latter conditions shall be implied in this paper by the term *wear resistance*.

The wear of a contact face is caused by the frictional forces that are present when sliding occurs. The structural characteristics of the contact specimen determine the resistance of the latter to wear when sliding occurs. Thus, the numerous variables of wear as evidenced by Figure 1 may be classified into two important groups—those which affect the frictional forces in one way or another, and those which affect the wear resistance of the contact specimen. The first group is associated with the whole sliding system while the second group concerns only the contact specimen itself. Of course, a particular variable may belong in both groups, as, for example, brush current. It is the means for measuring the effects of the second group on wear that are to be considered.

There is a philosophical truism that

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M. S. Coover is professor and head of the department of electrical engineering, and E. E. Jones is research assistant, electrical engineering department, engineering experiment station, both with Iowa State College, Ames, Iowa.

quency. Unfortunately, the power frequencies appear to be the most dangerous. Larger currents may be tolerated for both the higher and the lower frequencies and for direct current.

4. The public must be warned continually of the danger of accidental contact with power circuits and against using defective home appliances.

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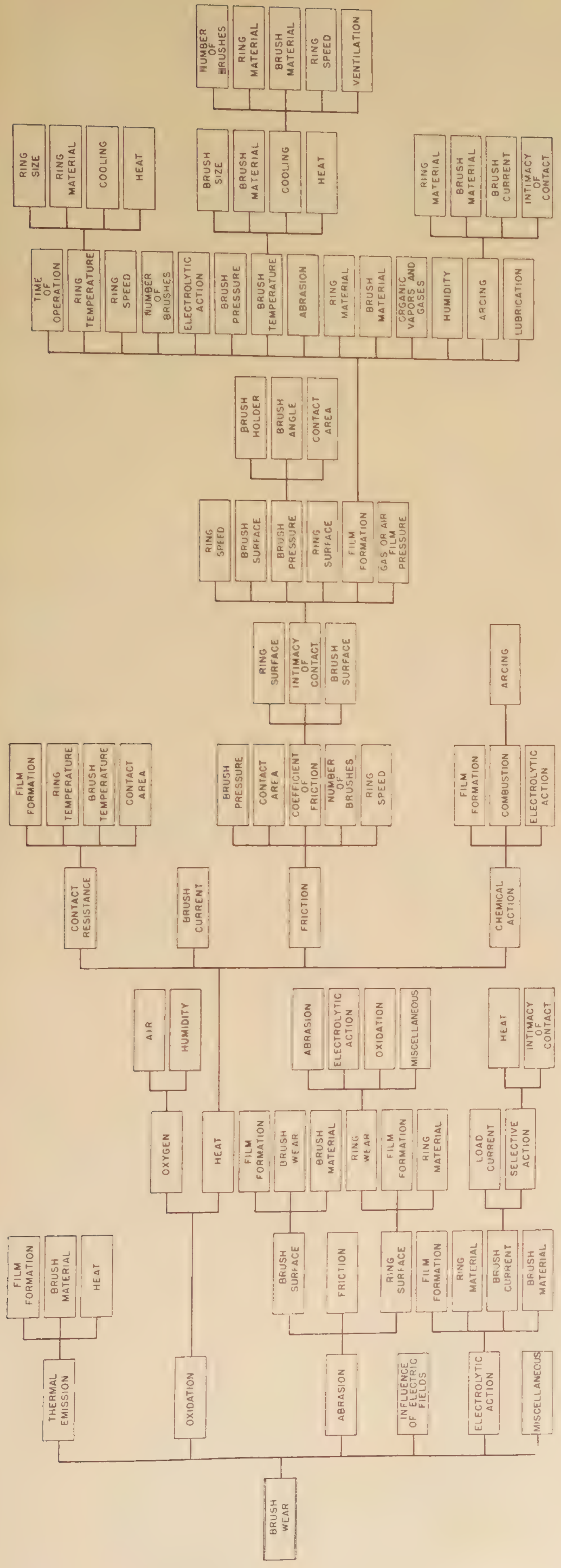

Figure 1. Elements of electrical-brush wear

asserts that the magnitude of any phenomenon is directly proportional to the magnitude of its cause and inversely proportional to the magnitude of that which resists or impedes the occurrence of the phemonenon. This truism is the basis for Ohm's law in the electrical system and for the analogous relationships in the magnetic and mechanical systems, respectively. It is proposed that it be applied to the phenomenon of wear. Since frictional force is the direct cause of wear, and the amount of wear is best measured as a time rate, it is suggested that that property whereby wear is resisted be defined so that the following relation will hold:

$$\text{Rate of wear} = \frac{\text{frictional force}}{\text{wear resistance}} \qquad (1)$$

It is believed that this equation represents a relationship that is just as fundamental and just as basic as is Ohm's law. In the light of this belief the efforts found in the literature to date to define wear resistance will be considered next.

It is interesting to note that the subject of wear has been studied from many different viewpoints, particularly in the field of metallurgy, and that many ingenious machines and other devices have been designed and built to test the wearing qualities of a variety of materials under controlled conditions that were intended to simulate actual working conditions. Only two papers have been found in the literature to date in which an attempt is made to define wear resistance by a mathematical expression. Neither of these expressions excludes wholly the properties of the external system. The first expression is defined by R. B. Freeman[4] as "unit wear," which he set equal to the expression obtained by "dividing the loss of weight experienced by the specimen during the run by the projected area of contact and the length of travel." This unit wear is measured in grams per square inch per foot and is expressed dimensionally as $ML^{-2}T^{-2}$. The chief disadvantage to this concept of unit wear is that it is a function of the density of the contact material being studied. Thus, if two specimens of differing densities should be tested under identical conditions and found to have the same volume of wear, the preceding expression would show the heavier specimen as having greater unit wear. It might be suggested that this definition be revised by substituting the loss of volume for the loss of weight. Such a ratio would then be equivalent to the ratio of length of end wear to the relative distance traveled by the contact faces and would be dimensionless. Both

definitions, however, are functions of the coefficient of friction and, therefore, do not provide a true comparison between different specimens for even the same contact area and contact travel. Thus, if one brush wears twice as much as another for the same ring travel and brush size, its unit of wear would be double that of the other, regardless of unequal surface conditions and other unlike conditions that influence the coefficient of friction.

The second expression that was found for wearing quality is defined by D. S. Clark and R. B. Freeman[5] as the energy required to overcome resistance to motion per unit volume of wear and is expressed dimensionally as $ML^{-1}T^{-2}$. The energy required to overcome resistance to motion, however, is a function of the coefficient of sliding friction as well as other properties that depend on the whole sliding system. The concepts of resistance to motion and of the energy required to overcome this resistance are treated later in this paper.

Table I

Electrical		Mechanical		Wear	
Inductance	L	Mass	M	Mass	M
Charge	Q	Length	L	Length	L
Time	T	Time	T	Time	T
Current	QT^{-1}	Velocity	LT^{-1}	Wear rate	LT^{-1}
Voltage	LQT^{-2}	Force	MLT^{-2}	Wear force	MLT^{-2}
Resistance	LT^{-1}	Resistance to motion	MT^{-1}	Wear resistance	MT^{-1}

Analytical

The frictional forces available for measurement are the tangential and normal forces, respectively, and the work done by (or against) each is the product of that force by the component of the relative motion of the contact member in the same (or opposite) direction of that force. Whenever force is applied to produce relative motion, the discrepancy between the applied force and the reactive forces due to the acceleration of the body is determined by frictional forces that produce heat and wear.

This statement may be represented by the following equation:

$$F=m\frac{dv}{dt}+K_dv \tag{2}$$

where F is the applied force, m is the mass of the system to which the force is applied, v is the velocity of the system, and K_d is the resistance to motion.

This may be illustrated by the motion of a streetcar on tangent level track during the accelerating period with constant torque output from the motor. The difference between the force applied and the summation of the frictional forces is the net force that produces acceleration.

The normal and tangential forces involved in sliding friction contribute mechanical energy to both wear and heat in unknown and quite variable proportions. The total energy delivered to the sliding contact is equal to the integral of the product of the average tangential force times the differential of the tangential distance traveled and of the product of the normal force times the differential of the depth of the wear or distance of cut, measured in the direction of the normal force. For ordinary sliding contacts where gouging is absent, the second product is negligible, while for the action of cutting tools the first product is negligible.

If resistance to motion be defined as

$$\text{Resistance to motion} = \frac{\text{frictional force opposing motion}}{\text{rate of motion}} \tag{3}$$

(or simplified this becomes $K_d=F/v$), then its dimensions are MT^{-1}, and the energy needed to overcome this resistance to motion may be defined as follows:

$$\int(\text{frictional force opposing motion})(\text{rate of motion})dt=\int(\text{rate of motion})^2(\text{resistance to motion})dt \tag{4}$$

Note the similarity of equations 3 and 4 to the electrical equations $R=E/I$ and energy $=\int EIdt=\int I^2Rdt$. Since the process of wear is brought about both by weakening the structure of the contact face to an unknown extent and by overcoming through sheer force an unknown part of the remaining strength of this structure, it is impossible to set up an exact expression for the energy absorbed by wear. To state it in another way, wear energy consists of heat, mechanical, and chemical energies and so far is beyond the reach of the experimenter's measuring devices. The definition of wearing quality given by Clark and Freeman may now be expressed as

$$\frac{\int(\text{frictional force opposing motion})(\text{rate of motion})dt}{\int(\text{projected area of contact})(\text{rate of end wear})dt}$$

Before wear resistance can be defined more explicitly than is shown in equation 1, it is necessary to determine what shall be meant by rate of wear. Rate of end wear can be measured in three ways:

1. Rate of loss of weight.
2. Rate of loss of volume.
3. Rate of loss of length (measured normal to the contact face).

It has been shown already that the rate of loss of weight is a function of the density of the contact material and is, therefore, unsuitable as the basis of comparison between unlike materials. The choice between rate of loss of volume and rate of loss of normal length can be made more easily by comparing two hypothetical contacts of the same homogeneous material, one of them having twice the contact area of the other. Reference to Figure 2 should assist with a visualization of how the hypothetical contacts may be expected to function.

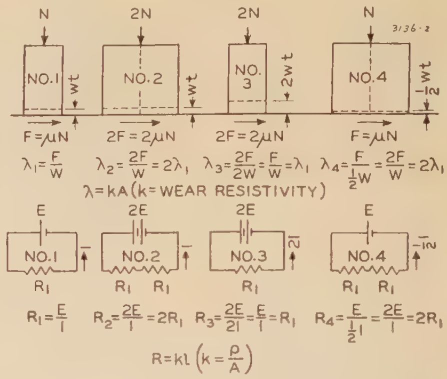

Figure 2. Analogy between wear and electrical resistances

It may be helpful at this point to compare the concept of wear resistance to that of electrical resistance. First of all, the R of Ohm's law ($I=E/R$) is a function of both the properties of the material and the dimensions of the electrical specimen. Secondly, that property of resistance to electrical conduction which is characteristic of the material alone is called resistivity and is related to R by means of the dimensions of the specimen. In like manner, it is desirable that wear resistance be dependent solely on the characteristics of the contact specimen, while *wear resistivity*, another new concept of wearing quality, be dependent solely on the properties of the contact material. The striking similarity between wear resistance and electrical resistance is shown by juxtaposition of their corresponding systems in Figure 2. It should be pointed out that this analogy between sliding contacts and electrical circuits has

the same limitation that all analogies have—that it can be carried too far, yielding some absurd results.

The requirement of identical conditions for these two hypothetical contacts implies that both of them must have the same normal pressure (say, pounds per square inch) and, hence, that the larger contact have twice the normal force of the smaller. Since both contacts are of the same material, their coefficients of friction (μ) may be assumed equal; therefore, the tangential force of the larger contact will be twice that of the smaller contact. Since the larger contact may be considered as two contacts like the smaller one fastened together, it may be assumed that both hypothetical contacts will have the same depth of end wear. Accordingly, the larger contact will lose twice as much volume as the smaller one. The limitation of this assumption will be treated later.

If the wear resistance be defined as the ratio of the tangential force to the rate of loss of normal length, the larger contact will appear to have twice the resistance of the smaller one. If, however, the wear resistance be defined as the ratio of the tangential force to the rate of loss of volume, both contacts will be indicated as having the same wear resistance. The first definition is a function of the properties of the whole contact specimen, while the second is a function of the properties of the contact material. Therefore, it has been chosen to call the ratio of the tangential force (F) to the rate of loss of normal length (W) the wear resistance (λ), whose dimensions are now MT^{-1}. Likewise, wear resistivity (k) is to be defined as the ratio of the tangential force to the rate of loss of volume (WA) and is expressed dimensionally as $ML^{-2}T^{-1}$. Therefore, it follows that

$$\text{Wear resistance} = (\text{wear resistivity})\ (\text{contact area}) \quad (5)$$

This can be clarified readily by reference to the four cases illustrated in Figure 2, in which the expressions for wear resistance are given. The expressions for wear resistivity for these four cases are, respectively, as follows:

$$k_1 = \frac{F}{WA} = \frac{\lambda_1}{A}$$

$$k_2 = \frac{2F}{W\cdot 2A} = k_1 = \frac{\lambda_2}{2A}$$

$$k_3 = \frac{2F}{2W\cdot A} = k_1 = \frac{\lambda_3}{A}$$

$$k_4 = \frac{F}{(W/2)2A} = k_1 = \frac{\lambda_4}{2A}$$

The analogy to a parallel-resistance electric circuit is a multiple sliding-contact system and will not be treated in this paper.

It is proper at this point to consider the limitations of the hypothetical contact when applied to actual cases. It being assumed that the foregoing developments are logical, it is considered perfectly valid to let these concepts of wear resistance

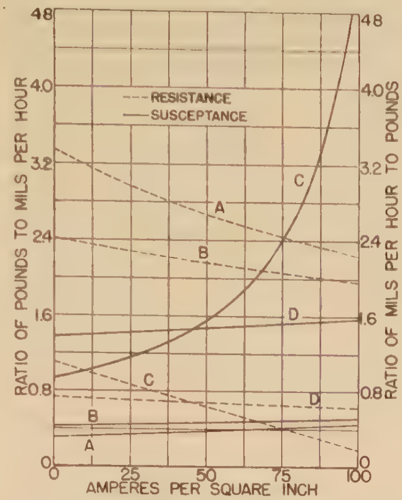

Figure 3. Wear resistance and wear susceptance versus current density for metal-graphite brushes—types *A*, *B*, *C*, and *D*

and wear resistivity absorb all of the variations in the physical properties of the contact member just as electrical resistance and electrical resistivity do for the case of electric conduction through any conductor. Thus, just as R under operating conditions is equal to

$$\frac{\rho_0 l}{A}\times K(\text{temperature})\times K(\text{stranding})\times K(\text{skin effect})\times K(\text{etc.})$$

so wear resistance may be set equal to

$$(\text{Wear resistivity})_0\times(\text{contact area})\times K(\text{contact temperature})\times K(\text{contact dimensions})\times K(\text{contact pressure})\times K(\text{sliding system})\times K(\text{ambient conditions})\times K(\text{etc.})$$

It should be pointed out that the adaptation coefficients for wear resistance as already given are not as simple as are those for the electric circuit. In the latter case these coefficients are both fairly simple and quite dependable. For wear phenomena, however, these coefficients are extremely variable and interdependent. A study of Figure 1 will verify this statement to a large extent.

The reciprocal of wear resistance, which may be called *wear susceptance*, has been found, in the case of electrical brushes, to be more easily understood than wear resistance. Since wear is the result of friction, it is more intelligible to think of the rate of wear per unit of friction than of the amount of friction per unit of wear rate. Likewise, the reciprocal of wear resistivity, which may be called *wear susceptibility*, is more useful than is wear resistivity.

Experimental

The curves shown in Figures 3 and 4 for four kinds of metal-graphite brushes were calculated from experimental data obtained at the Iowa Engineering Experiment Station and represent the average of repeated tests at each current density between 0 and 100 amperes per square inch at intervals of 20 each. The current used was 60-cycle alternating current. The measured data included frictional torque of the test brushes, their wear, and the spot temperature in a representative brush of each group tested. The wear is measured in mils and the wear rate in mils per hour.

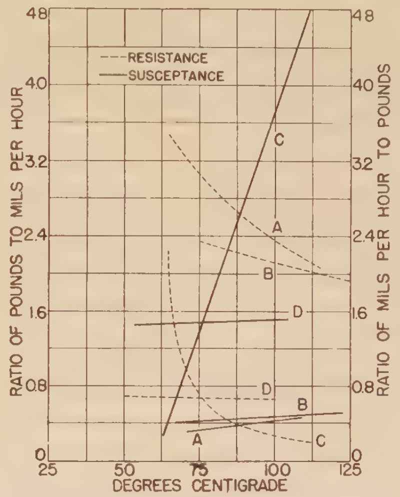

Figure 4. Wear resistance and wear susceptance versus spot temperature for metal-graphite brushes—types *A*, *B*, *C*, and *D*

The test equipment consisted of two units of similar construction, each having a slip ring of standard material driven by a separate motor. Each slip ring, together with its brushes, was enclosed so as to permit the control of atmospheric conditions. Eight brushes were placed radially on each ring with a normal pressure of three pounds per square inch of contact area and were tested at an average speed of 4,200 feet per minute.

The duration of each test run was the same, and data were recorded at regular intervals throughout each run. All values except brush-wear data used in the cal-

culations of the curves of Figures 3 and 4 represent the averages of these recorded data for a set of runs at each current density except for that obtained during the first hour of each run. This first hour's data were influenced by transient conditions that accompanied the start of every test run, but the rest of the data for each run were, for the most part, steady state. Both test units, which had previously been compared and checked for similarity of results on like brushes, were operated simultaneously in all runs. The brushes were tested as follows:

Types *A* and *B* simultaneously on units 1 and 2, respectively.

Types *C* and *D* simultaneously on units 1 and 2, respectively.

Each series of tests was preceded by a process of stoning and sanding followed by an operating period of sufficient length to wear in the brushes properly and establish a well-developed characteristic ring film. The film was henceforth undisturbed until the series of runs had been completed, and it was necessary to make a similar preparation for the next set of brushes. Experience has shown that the film formation on a ring is a function of the brush current as well as of other operating conditions and that large changes of current density produce results that are not characteristic of either the original or the final current density. It was found, however, that an increase of current density in steps of 20 amperes per square inch each permitted tests that were wholly reliable within the limits of experimental error. Several runs were conducted at each current density before proceeding to the next.

The frictional force, which is measured in pounds, was obtained by converting the electrical input to the driving motor into the torque required by the brushes. The efficiency data required for these conversions were revised for every test run, so as to prevent, insofar as possible, the errors due to the variable losses of the test equipment.

The brush-wear data were obtained by making four micrometer measurements, each to the nearest 0.0001 inch, on each of the eight brushes, before and after each run. The difference between successive readings for each of the 32 points helped to determine the average brush wear for that set of brushes.

The spot temperature was measured by a thermocouple junction placed in a hole drilled in the top of one brush in the group being tested. It has been found that consistent use of the same point in all tests gives approximately the same comparative results as a group of thermocouples placed at various accessible points on a brush. This temperature, which is plotted as abscissa in Figure 4, does not, however, represent the true temperature of the contact face. There are three important temperature ranges in a sliding contact member—the body temperature, the face temperature, and the local spot temperatures at the points of most intimate contact. Each of these ranges is exceedingly variable, both in intensity and extent. It is a truly difficult problem to obtain a true picture of the temperature conditions in the contact and its members. Nevertheless, it is believed that the curves shown in Figure 4 are not totally devoid of significance and that they may be used for rough but very useful comparison.

The values of wear resistance and wear susceptance are plotted against current density for practical reasons and against temperature for theoretical reasons. Practically, the similarity of the curves of wear susceptance and wear resistance in Figure 3 to those obtained for rate of brush wear and its reciprocal, brush life, respectively, indicates that friction alone was not responsible for the variations in wear rates. The phenomenon of wear is a function of both its cause, which is friction, and the binding strength of the material, which restrains it to a varying degree. The binding strength is, in turn, a function of the temperature of the brush material, weakening as the temperature increases. Since the brush temperature increases with an increase in current density, it is reasonable to expect the ratio of the wear to the friction to increase, likewise.

Theoretically, the curves of Figure 4 indicate the relative strength or weakness of the binding materials of the respective brushes for similar ranges of temperature. It is easy to recognize the superiority of the binding materials in brushes *A* and *B* and the great inferiority of the binder in brush *C*. It should be noted again that this ratio of wear to friction is almost completely independent of the external system and is almost wholly a property of the brush itself.

Conclusions

The development of concepts of wearing quality based on the truism that the magnitude of a phenomenon is proportional to its cause and inversely proportional to that which resists it has been given, and two of these concepts are shown for the actual cases of four kinds of metal-graphite brushes. Since electrical resistance, resistance to motion, and wear resistance are based on the above truism, it is no surprise that electrical, mechanical, and wear phenomena should be analogous to some extent. The table shown below illustrates the extent of the analogy for all three phenomena insofar as can be determined at present.

It is believed that these concepts, together with the chart of variables affecting brush wear shown herein, provide a more solid foundation for the furtherance of the study of wear of electrical brushes. It is recognized that a subject so inherently complex as an electrical sliding contact cannot be made simple. It is possible, however, to analyze the total phenomenon into its component parts and, where these component parts are also exceedingly complex, to analyze them further into simpler components, repeating the process until components that can be determined readily are obtained. The first step in this process of analysis has been suggested in this paper by the separation of the characteristics of the contact specimen itself from those of the outside system.

Apart from abstract analysis, however, wear resistance, wear susceptance, wear resistivity, and wear susceptibility are believed to provide new and important tools for the comparison of brushes or other materials used in sliding contacts.

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Parallel Operation of Airplane Alternators

D. W. EXNER
ASSOCIATE AIEE

Synopsis: The increase in airplane electrical loads has made necessary an increase in the distribution voltage to avoid excessive copper weight. Difficulties in the use of higher d-c voltages are mentioned, and past experience with a 400-cycle three-phase system is outlined. In the light of this experience and of new developments in progress, requirements are outlined for a larger 400-cycle system with multiple generating units operating in parallel.

ELECTRICAL loads on military airplanes have increased tremendously in the last three years because of the substitution of electric power for hydraulic power to drive auxiliaries, or the use of motor-driven instead of engine-driven hydraulic pumps, and because of the development of new flight and combat devices.

The Trend Toward Higher Voltage

First in use because of its simplicity, the d-c system progressed through the 6- and 12-volt stages until, in 1939, new airplanes were designed with a 24-volt system.[1] As the loads increased, the weight of wiring required for the lower-voltage systems became prohibitive. Now, in 1943, the economical limit of the 24-volt system has been exceeded, with new airplanes demanding 1,600- to 2,000-ampere peak loads. A further increase in voltage is therefore necessary. Because of the continued upward trend of the load-demand curve, and because of the increasing transmission distances in the larger airplanes, the next step will be above 100 volts.

Unfortunately a change to 115 volts direct current on high-altitude airplanes introduces at least one problem to which a completely satisfactory answer has not yet been found. This is the difficulty of arc rupturing in the switching equipment at present and contemplated flight-altitude maxima for combat airplanes. Fault currents may be higher than for a-c equipment of equivalent rating. On small motors at this voltage, commutation at high altitudes is another problem. Partly for these reasons, the United States Army Air Force has selected a 208/120-volt 400-cycle three-phase four-wire system for the large combat airplanes now under development.

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D. W. Exner is control design engineer, small motor division, Westinghouse Electric and Manufacturing Company, Lima, Ohio.

The author acknowledges the assistance of C. G. Veinott and H. E. Keneipp in the discussion of alternator characteristics and performance.

The XB-19 Installation

Early experiments by the Air Corps with a-c auxiliary power on the *XB*-15 resulted in procurement of auxiliary-engine-driven 400-cycle three-phase power plants for the *XB*-19. Figure 1 is a schematic view showing the circuit arrangement. The design of the engine and alternator was co-ordinated to result in the

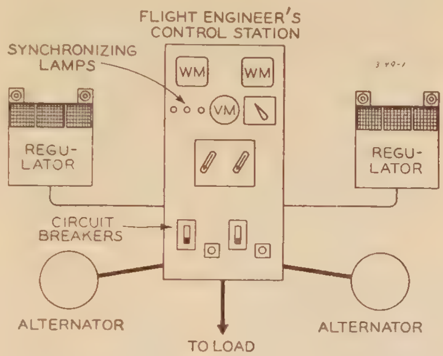


Figure 1. Schematic view of the auxiliary power plants used on the *XB*-19 airplane

package unit shown in Figure 2. The pancake proportions of the alternator do not give the optimum weight of electrical parts, but, in conjunction with the aluminum fan, provide the necessary flywheel effect for the engine. The use of 14 poles gives a synchronous speed of 3,430 rpm. The short-circuit ratio is 0.60, a figure which may seem low to a designer of 60-cycle machines, but which has proved satisfactory in operation. Heavy dampers with continuous end rings limit oscillation caused by engine-torque pulsation. They also help support the voltage until the regulator responds to a load increment. With a self-excited system, this materially assists in prompt recovery of voltage if the regulator is fast enough to take advantage of it.

The design rating of the alternator is 120 volts, 12.5 kva, 0.8 power factor, although, to conform to the Air Corps specification, the name-plate rating is 10 kva, 0.75 power factor. As the system is operated ungrounded, the neutral is not brought out.

The rotating field of the alternator is designed for overhung mounting on the engine crankshaft, which makes the use of a d-c exciter difficult. Chiefly for this reason, it was decided to use self-excitation. An electronic voltage regulator, using *RO*-585 diodes to control a three-phase half-wave thyratron rectifier, provides the fast-acting field supply so necessary in a self-excited system.

The *RO*-585 filaments are operated from a positive-phase-sequence network which delivers a single-phase voltage proportional to the average of the root-mean-square values of the three line voltages. As the regulating action is a function of the filament temperature in these tubes, the regulator is responsive to the average of the three root-mean-square phase voltages. An incidental advantage in using the positive-sequence network is the accomplishment of excellent temperature compensation by using in the resistance element of the network a material having a positive temperature coefficient of resistance.[2]

The flight engineer's panel, the installation of which is shown in Figure 3, contains the instrumentation and manual controls. The panel compartment houses the voltage build-up and reactive-load-equalizer circuits, the wattmeter current transformers, and the manual circuit breakers. Voltage is built up initially from an alternator residual voltage of approximately nine volts by means of a copper-oxide rectifier and a small multicontact Silverstat voltage regulator, both of which are intermittently rated to supply no-load excitation. A small high-reactance step-up transformer overcomes the initially high resistance of the rectifier without resulting in excessive excitation at normal voltage.[3] By this means the voltage overswing is reduced. For each alternator a three-position multicontact manual cam switch controls the starting sequence. This switch must be in the off position before the auxiliary-engine-starter circuit can be completed. With the engine at normal speed, the switch may be turned to the warm-up position, which allows the voltage to build up and be maintained under the control of the auxiliary regulator. After 45 seconds a thermal relay energizes a tripping magnet on the cam switch, automatically advancing the switch to the run position, although it may be returned to the off position at any time. The two cam switches are mutually interlocked to prevent an attempt to build up both alter-

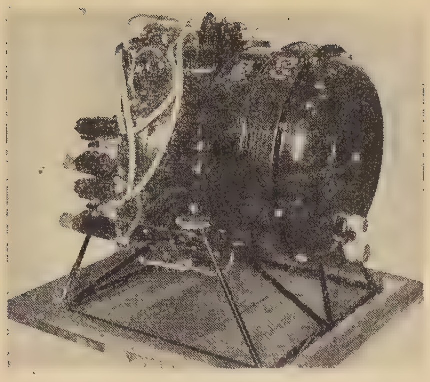


Figure 2. The 12.5-kva alternator assembled on the Ruckstell-Burkhardt auxiliary engine

nators at the same time, as only one build-up circuit is provided. As soon as one alternator is transferred to electronic regulation, it may be connected to the load bus by closing its manual circuit breaker, although, particularly at low ambient temperature, it is desirable to operate at no load for a few minutes to allow the thyratrons to reach normal operating temperature.

Operating Experience on the XB-19

With no load on the first alternator, and with the governors of the two engines closely matched, it has been found fairly easy to synchronize manually at full voltage on every attempt when the synchronizing lamps are flashing not faster than three cycles per second. At rates of slip up to six or eight cycles per second, it is possible to synchronize on approximately 75 per cent of the attempts with random closure of the breaker. With more than two or three kilowatts of load on one alternator, it is necessary to close while the lamps are dim or dark. Test experience with these power plants indicated the necessity of manual control of the engine governors, and this feature was added when the equipment was installed in the airplane. After some practice, and with the engines in smooth running condition, it was then possible for the operator to synchronize with full load on one alternator, although he would occasionally miss. When the engines have been run for a number of hours after an overhaul, momentary speed changes during attempts to synchronize make it difficult to do so with more than six kilowatts of load.

During the development stage, tests were made to determine if it were practical to synchronize by bringing the incoming alternator up to approximately synchronous speed with zero or weak field and allowing it to pull into step by means of its damper winding and reluctance torque. This attempt was not successful, because the highly reactive load severely overloaded the first alternator. While it is true that better results would have been obtained with separately excited machines, the shock would still be prohibitive when attempting to synchronize under load.

When paralleled units are driven by auxiliary engines, it is necessary to cut off promptly an alternator whose engine has failed to carry load. Magneto trouble, fouled plugs, and vapor lock, or other fuel-system faults, will occasionally cause a short-time or sustained power failure. Any tripping means for isolating such a unit should have its action delayed sufficiently to prevent operation on a momentary misfire which can be sustained by the inertia of the machine. On this equipment, protection is afforded by a reverse-power contact on the wattmeter which actuates a shunt trip magnet on the circuit breaker. This contact is set at three kilowatts, and the damping of the instrument gives the necessary delay. A small pilot relay inside the instrument case relieves the pointer contact of the tripping current.

Experience with the system has demonstrated the desirability of a uniform response rate of the several governors. If the rates differ materially, one engine tries to drive the other on the sudden removal of a large load, and faulty tripping of the reverse-power device may result. A particularly bad performer is a governor which has sluggish response in correcting overspeed. Although the condition was reproduced during preliminary testing, no serious trouble has been experienced in service operation.

Experience with the scheme for automatically building up the alternator from its residual voltage has been generally good but subject to occasional failure caused by a reduction of normal residual flux. This usually follows a shutdown caused by loss of voltage resulting from a highly inductive overload, and may occur after an engine failure has transferred all

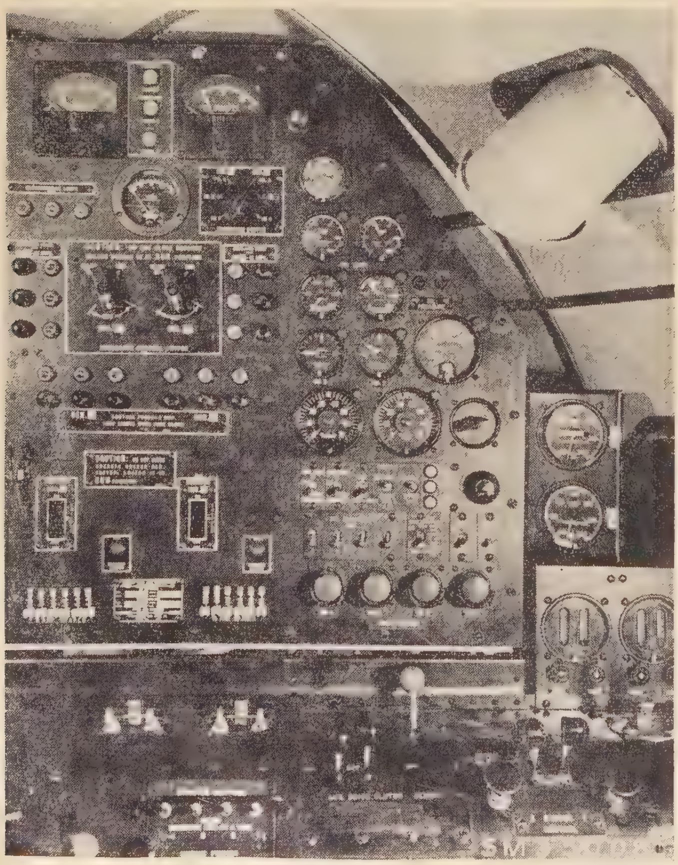


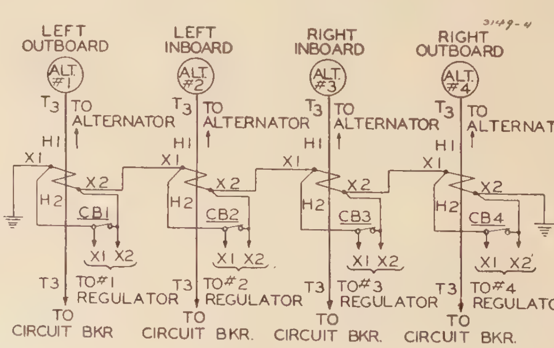
Figure 3 (left). Alternator and auxiliary-engine instruments and manual controls at the flight engineer's position of the *XB*-19 airplane

The two plunger knobs in the lower right-hand corner of the picture permit manual control of engine speed while synchronizing

Photo courtesy of Douglas Aircraft Company

Figure 4 (below). Standardized current-transformer interconnections for feeding the reactive load-equalizer networks of the voltage regulators

Agreement on polarities is necessary to permit interchanging regulators of different manufacture



of the load to the remaining alternator. It may also be discovered after the flight engineer has mistakenly shut down an engine without first opening the alternator circuit breaker. To overcome this condition, a circuit has been added which permits the operator to "flash" the field with battery voltage if necessary.

Requirements for a Modern A-C Installation

Having examined an existing a-c installation, we may proceed to lay down the requirements of an airplane power system which will take advantage of the experience gained and the newer developments which are available. It cannot well be denied that a successful system must involve parallel operation of a number of generating units. It is not acceptable that the loss of a single unit can shut down the whole system. The loss of one unit out of two is nearly as bad, unless the essential load is within the rating of one generating unit. This condition might exist in a noncombat airplane, but the greater hazards of combat operation dictate the use of at least three and preferably four units where the size of the airplane permits. Nonparalleled or isolated operation of the several generating units is a makeshift which does not use to the full the power capabilities of the system in starting heavy motor loads and self-clearing of faults; that is, the load factor with separate units is poor. Loss of a generating unit results in a delay before the load can be reapportioned among the other units.

The ability to parallel alternators depends first on the characteristics of the prime movers. The earlier a-c aircraft systems used auxiliary gasoline engines governed to run at essentially constant speed. The governors are designed to droop the speed slightly as load increases, which characteristic automatically gives acceptable division of kilowatt load between units. As the capacity of the prime mover is comparable to that of the alternator, and the inertia is relatively low, no difficulty has been experienced in maintaining synchronism under all conditions, including load transients. In the *XB*-19 equipment, the use of low-resistance damper windings effectively prevents hunting caused by engine-torque pulsations. Whereas the use of auxiliary engine drive may be satisfactory for medium-altitude transport and cargo airplanes, at the present time it seems to be impractical to supercharge the smaller engines to a critical altitude sufficiently high to permit their use on high-altitude bombers.

Constant-Speed Drives

Until recently, no suitable means was available for driving alternators from the main engines and at the same time permitting parallel operation. Now several so-called "constant-speed" drives are under intensive development and promise to have the necessary performance. The input–output speed ratio is continuously variable, so that a constant output speed may be maintained over a wide range of input speed. This development gives the a-c electric-system prime movers of proved reliability and high-altitude performance.

A number of difficulties must be overcome in adapting constant-speed drives to this application. Because of the large size of the main engine in proportion to the rating of the alternator, the engine is a rock-steady source whose speed is practically unaffected by alternator loading. On the other hand the engine speed may suddenly be changed because of flight requirements; yet the alternator should not perceive this change. As a result, the drive or its governor must incorporate the load-dividing feature, and the governor must be quick in coming into action and be able to change the drive ratio at a rate faster than the engine can accelerate. In a speed-converting mechanism the difference between the input and output torques appears as a torque reaction on the housing, and it appears to be unavoidable that this reaction torque is transmitted principally through the ratio-changing mechanism. Consequently at high ratios the ratio-changing device must develop considerable power to alter the ratio.

The necessary rate of change of ratio is determined by the maximum possible rate of acceleration or deceleration of the engine. Information from one of the engine manufacturers states that an acceleration rate of 3,000 rpm per second may be found in normal operation, although it is considered detrimental to engine life, and that a rate of 3,500 rpm per second is possible under some combat conditions with hand control of the propeller pitch. The drive should be capable of responding at a rate somewhat faster than this to have a reasonable factor of safety. It seems probable that the engine deceleration rate will be appreciably less than 3,000 rpm per second, although no confirming data are known to the author.

Because of the rigidity of the prime movers, it is essential that the drives incorporate overrunning clutches to permit the alternators to remain in synchronism in spite of momentary deviations in drive ratios or failure of the drives to compensate promptly for rapid changes in engine speed. Partly for the same reason, it is desirable that the drive have some internal slip, either inherent or introduced by the use of a slip coupling such as a fluid flywheel.

Unless the drive has an inherent slip which is consistently reproducible and is independent of the ratio setting, it is necessary that the governor be made load-responsive to provide the speed droop which is required for division of kilowatt load between paralleled alternators. Methods are known for obtaining load division by controlling the relative phase position of the alternator rotors without producing a change in the steady-state speed, but they lack the simplicity and reliability of the speed-droop method. Though the governor may be the droop-determining means, it will have some delay in adjusting itself to a new load level. For this reason some inherent slip in the drive will assist in the maintenance of synchronism during sudden large load transients. Internal slip will also reduce the effect of engine and drive torque pulsations.

While the governor must have a rapid response rate, it must also be well damped to obtain system stability during load and fault transients. Keeping the force-inertia quotient of the ratio-changing device high will make the damping problem easier.

Alternator Design

Experience with the self-excited a-c system has demonstrated a degree of overload capacity and system stability inferior to that which may be expected with separate excitation. The load characteristic of the self-excited machine has a definite knee beyond which the voltage is not self-supporting, with the result that a suddenly applied heavy load may cause loss of voltage, whereas the same load gradually applied may be sustained. Synchronizing difficulty is increased, and the ability to burn minor faults clear or blow limiters is distinctly reduced. Alternators of current design have integral d-c exciters to overcome these handicaps.

Low-resistance damper windings are necessary to ensure freedom from oscillation excited by driving torque pulsation or load transients. Some assistance is given in synchronizing by the full-voltage method because of the damping effect on oscillations caused by closing the circuit breaker at inexact synchronous speed and phase position. When synchronizing is performed by the synchronous motor

method, that is, bringing the incoming machine up to approximate speed with no field or with weak field and closing the breaker, low-resistance damper windings are essential, and some benefit is obtained by using continuous end rings. The latter feature will also increase the ability of the alternator to pull back into step automatically under some conditions of out-of-step operation.

It has been suggested that aircraft alternators should be designed with a short-circuit ratio around unity because 60-cycle practice has shown that this gives high inherent stability which will be a benefit in parallel operation and in starting heavy motor loads. The suggestion has merit, but unfortunately it results in a heavy machine. Much of the benefit may be obtained with lighter overall weight by increasing the capacity of the exciter to supply heavy excitation when needed and by making the regulator as fast as possible. Satisfactory experience with self-excited alternators having a short-circuit ratio of 0.60 discounts the necessity of accepting the higher weight of the design which has a higher ratio.

The Voltage Regulator

The use of an exciter brings the regulator duty within the capacity of existing designs of aircraft d-c regulators which have undergone extensive development and combat service during the past two years. What modifications are necessary can readily be made to adapt them to the requirements of the a-c system. If the operating coil is supplied from a three-phase dry-plate rectifier, the regulator will respond to the average of the three average phase voltages. The fact that this is an average rather than a root-mean-square response is not expected to be objectionable with alternators whose harmonic voltage output is within the limits set by the Army Air Forces tentative specifications. The tentative specification for the voltage regulators recognizes the necessity for this average response by stating that the voltage will be checked on type test with a rectifier-type voltmeter calibrated to read root-mean-square values of sine-wave voltages.

The regulator includes a network designed for use in a series-type differential reactive load-equalizer circuit for causing the regulators to equalize the circulating reactive current between alternators operated in parallel. The network is connected to a 125/1-ratio current transformer in the $T3$ lead from the alternator. Zero-power-factor lagging current from the transformer develops across the network a voltage which is in phase and in series with the line voltage applied to the regulator-coil-circuit rectifier. Since this increases the voltage applied to the regulator coil, the regulator acts to decrease the alternator excitation. Conversely, the presence of zero-power-factor leading current will cause the regulator to increase the excitation. Inphase line current produces a network voltage at a right angle to the voltage applied to the regulator and therefore does not affect the operation. Figure 4 illustrates the differential equalizer circuit which has been selected as standard by the interested manufacturers to ensure interchangeability, and this has been incorporated in the tentative specification by the Army Air Forces. With this connection, if the line currents of the respective alternators are equal in magnitude and phase angle, the transformer secondary current circulates solely through the secondary circuits and produces no voltage drop across any of the regulator networks. If one machine delivers more than its share of current, the secondary difference current divides proportionately in the parallel impedance paths formed by the regulator network of the overloaded machine as one branch and the other regulator networks in series as the other branch. With an excess of lagging current delivered by the first alternator, the phase angle of the network voltages in the two branches is such that the excitation of the overloaded machine is reduced and that of the others is increased, with the result that the reactive load balance is restored without affecting the voltage of the system. This lack of effect on the system voltage while correcting a reactive-load unbalance is the advantage which the differential method holds over the somewhat simpler scheme in which balance is achieved by drooping the voltage of each alternator in proportion to the lagging reactive component of current which the machine is delivering, thus producing a droop in system voltage. The differential scheme is identical in principle with the load-equalizing method which is used in present aircraft d-c systems. An improvement over the d-c circuit is the use of a normally closed interlock on the alternator circuit breaker to short-circuit the secondary winding of its associated current transformer when the breaker is open, and so avoid a reaction of the idle alternator circuit upon the regulated voltage of the other loaded machines. To be effective always, this interlock should be designed with the knowledge that it must effectively short-circuit a low voltage. Proper choice of contact materials and contact pressure and the use of several contact points in parallel will avoid trouble caused by contact film and dirt.

Special antihunting or damping means are necessary in the a-c voltage regulator because of the additional field circuit delay which is introduced by the use of an exciter. A very effective method uses a transformer to introduce into the d-c circuit of the regulator coil a voltage proportional to the rate of change of exciter voltage or the rate of change of alternator field current. When introduced with the proper polarity, this voltage causes the regulator coil to anticipate the response of the line voltage, which effect tends to damp out a tendency to hunt or overshoot.[4]

Choosing a Synchronizing Method

The choice of a synchronizing method is somewhat dependent on the prime movers which are used. If the system is driven solely by constant-speed auxiliary engines, a manual or semiautomatic method may be acceptable, because synchronizing is performed infrequently and at a time when the flight engineer is less occupied with other duties. It may be permissible to drop all or part of the load before synchronizing. With main-engine drive, on the other hand, fully automatic synchronizing is definitely desirable, because the availability of electric power is dependent on engine speed conditions, which in turn are determined primarily by warm-up, ground-maneuvering, and flight requirements. When the several alternators come up to speed, the flight engineer may have other duties requiring his attention. With automatic synchronizing, each alternator becomes available for loading as soon as it reaches synchronous speed.

Automatic Synchronizing

Automatic synchronizing equipment has been used in unattended substations and small power stations for many years with excellent service records. For aircraft use the same principles can be applied, but many refinements may be eliminated because of the presence of supervision and the necessity of saving weight. The essential features may be stripped down to means for performing the following functions:

1. Determine when the alternator frequency has approached normal, and initiate the succeeding sequence.
2. Indicate whether the bus is dead or already energized. If it is dead, initiate circuit-breaker closure; or if it is alive,

initiate the synchronizing sequence as follows:

3. Act on the incoming governor to adjust the speed until the difference frequency approaches zero.

4. Close the circuit breaker when the difference frequency is less than about two cycles per second and the alternator and bus voltages are closely in phase.

5. Advance the governor to the normal load setting.

6. Open the circuit breaker at any time when the system frequency drops below the acceptable minimum.

Automatic synchronizing by this full-voltage method may be accomplished with a load on the system, because the human elements of indecision and reaction time are eliminated from the operation. Nevertheless manual controls are provided so that the flight engineer may select the units to be paralleled, or he may trip any alternator circuit breaker at any time. After a machine has been connected to the bus, the flight engineer may, at his leisure, correct any inequality in load division or error in frequency by a manual control which acts through the speed-matching device to adjust the governor setting. Thus he has full control over the operation of the system but is relieved of the necessity of performing the somewhat critical operation of simultaneously matching speed and phase position and closing the breaker at the correct instant. With four alternators he would otherwise have to go through this cycle three times in succession shortly before the time of take-off.

Manual Synchronizing

The weight of the control equipment can be reduced by using manual instead of automatic synchronizing methods, but the saving in weight must be balanced against the increased burden on the flight engineer, the increased chance of an outage caused by faulty manual technique, and the decreased availability of the generating equipment because of failure to connect it to the bus promptly. Perhaps the least objectionable in these respects is a semiautomatic system which differs from the one previously described in that the automatic speed-matching feature is replaced by a manual control. This arrangement requires the attention of the flight engineer at each synchronizing operation but greatly eliminates the factor of faulty manual technique, because the automatic means for determining when the breaker should be closed are retained. The weight saving in comparison with the fully automatic system is approximately two pounds per alternator.

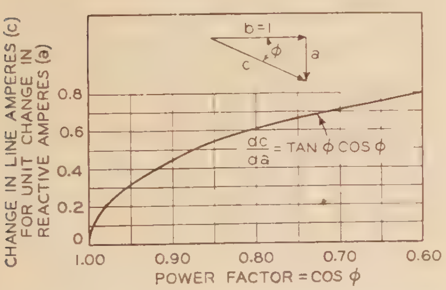


Figure 5. Rate of change of ammeter reading with respect to the reactive component of current plotted against power factor

At normal power factors the ammeter is an insensitive indicator of correct voltage-regulator equalization

Of the straight manual synchronizing schemes which have been proposed for airplane use, perhaps the most foolproof is the one in which the alternator is synchronized in a manner which is common for synchronous motors. The incoming machine is brought up to slightly below synchronous speed with zero or weak field, the circuit breaker is closed, and the machine accelerates toward synchronous speed because of the motoring effect produced by its damper winding. When it is close enough to synchronous speed, it will be pulled into step by its reluctance torque. If this is done with no field excitation, there is a 50–50 chance that it will pull into step with the wrong pole position and will have to slip a pole pitch when field is applied. If the breaker is closed with weak field on the machine, the pulsating current will be higher, but the machine will pull in with the correct pole position. An advantage in starting with weak field lies in the fact that synchronizing lamps may be used as an indication of slip speed while the speed is being raised by manual adjustment of the governor before the breaker is closed.

An important disadvantage of this synchronizing method is the high motoring current which is drawn even at a low slip. Calculations on the present design of airplane alternator indicate that this will equal rated full-load current at about one per cent slip and 160 per cent of full-load current at about five per cent slip. Furthermore, this current pulsates badly at slip frequency. The current calculations were made with the assumption of an infinite bus, and the actual current will be somewhat less. This will increase the time of the system disturbance. The nature of this effect is substantiated by tests which were made during the development of the equipment for the *XB-19* airplane. With one machine on the bus, synchronizing the second machine by this method will be difficult with a load of more than 50 per cent of the alternator rating. It may be found necessary in practice to remove the system load before synchronizing the second alternator.

Adjusting the Voltage Regulators

Experience has demonstrated the difficulty of convincing airplane crews that kilowatt load in an a-c system cannot be balanced between paralleled alternators by adjustment of the voltage regulators. Having had instruction and more or less extensive experience with d-c systems, they must be shown by example that the two systems are not alike in this respect. As serious misadjustment may be encountered, and as this will have an adverse effect on stability because of unequal field excitation, the process of regulator adjustment must be made as simple as possible.

The fact that ammeters are not good instruments to follow when adjusting the

Figure 6. Mock-up model of 40-kva alternator of current design

regulators is emphasized by Figure 5, which shows how the ammeter reading varies with the changing reactive current, the inphase current remaining constant. At 0.75 power factor the ammeter responds two thirds as fast as the reactive component, whereas at 0.87 power factor the response is only one half as fast as the change in the reactive component. The matter is further complicated at small loads by the fact that a-c ammeter scales are condensed in this region. At normal power factors the inphase component of current has a greater effect on the ammeter reading than the reactive component does, so that a change in power factor or a change in load balance caused by governor aberrations while the regulators are being adjusted will be extremely confusing.

Because regulator adjustment directly affects the reactive current balance between paralleled machines, varmeters (which read "volt-amperes reactive") give a positive indication of the condition of

regulator equalization. Not being responsive to real or kilowatt load, they will not give confusing readings because of governor misadjustment. A varmeter is nothing but a wattmeter with a reactive compensator to shift the potential coil voltage 90 degrees from its true position. Since wattmeters are already necessary in the system to permit supervision of loading and adjustment of the governors, the addition of reactive compensators and a simple switching arrangement make them useful for regulator adjustment also.

Conclusions

A background of experience exists to guide the development of 400-cycle power systems for airplanes, and this knowledge is being used to advantage in the present program of expanding the installed capacity. To duplicate the reliability of the present d-c system, parallel operation of the alternators is required. Parallel operation of auxiliary-engine-driven units already has been accomplished, and, given constant-speed mechanical drives with the correct characteristics, this performance can be duplicated with main-engine-driven alternators. The numerous duties of a flight engineer on a multiengine airplane make it highly desirable to provide automatic synchronizing control to increase the availability of the power system and eliminate outages caused by man failure during the synchronizing operation. Equipment for doing this is available or is being developed.

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Principles of Aircraft Electric-Motor Protection

V. G. VAUGHAN
NONMEMBER AIEE

Introduction

In writing a paper of this kind for the AIEE, it is needless to say that one part of the aircraft electric-motor protection problem is protection from excessive temperatures. The major problem with the aircraft electric motor is to make available its maximum output before the protector disconnects the aircraft electric motor from the line.

The problem involved in applying an aircraft motor protector for maximum output under all conditions may be realized when it is considered that today an aircraft may be stationed on a field in the tropics and within 24 hours it may be moved to a field well within the Arctic Circle. Or, even more striking, an aircraft may be stationed on a field in the tropics with the sun beating down so as to cause temperatures as high as 70 degrees centigrade in compartments where electric motors are located. In a few minutes this aircraft may be flying at a high altitude where the ambient temperatures may be as low as −60 degrees centigrade.

In domestic, commercial, and industrial uses of electric motors, it has been the aim to protect electric motors so they would not be personal or fire hazards. In accomplishing this, it has been good engineering practice to provide an ample margin of safety. In October, 1942, C. P. Potter presented a paper before the AIEE entitled "The Inherent Overheating Protection of Single-Phase Motors" from which I quote his comments regarding the summary of the data for the tests under various loads, voltages, speeds, ambients, and amounts of ventilation:

"Conclusions

The variations in temperature shown in the table are small and lead to the conclusion that inherent overheating protective devices mounted inside fractional horsepower single-phase motors completely protect them against all abnormal operating conditions."

There are many engineers who believe that all motor protectors will limit the work output of electric motors, and they advocate that the operator should have the privilege of destroying the aircraft electric motor if by so doing he thinks he can accomplish his mission. It is questionable if any operator has the ability to judge properly during the stress of emergency as to when he should or should not destroy a motor.

A good automatic device will always repeat its performance, and in this paper it will be assumed that a properly designed and properly applied electric-motor protector must not place any limitations on the potential work output of the electric motor.

Stated in the simplest terms, the problem is to establish principles to follow in protecting aircraft electric motors from destructive temperatures without limiting output regardless of the loads or the ambient encountered.

While there are many classes of insula-

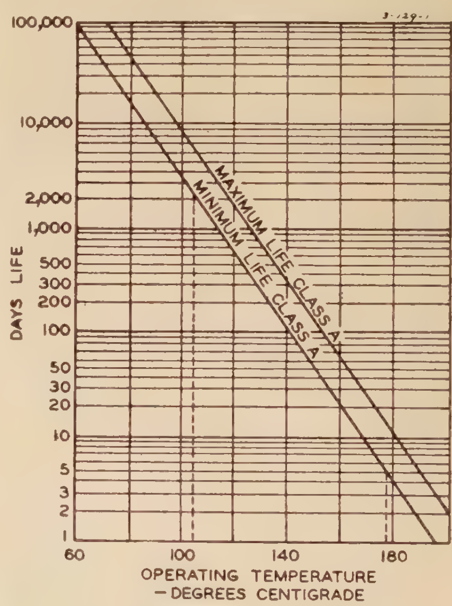

Figure 1. Effect of temperature on life of insulation

tion in use in aircraft electric motors, many of these electric motors have some class A insulation in them. There is more information available on class A insulation, and for the purpose of this paper the consideration of any other class of insulation would be a matter of degree and would not change the principles to follow in having protection without limiting output.

Since the first use of electric motors, there has always been this problem of temperature protection. First, fuses were used, then current-sensitive devices mounted remote from the electric motor, and since 1929 there has been an increasing use of built-in electric motor protectors. E. J. Schaefer, formerly of the

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V. G. Vaughan is with the Spencer Thermostat Company, Attleboro, Mass.

fractional motor engineering department of the General Electric Company, in a paper entitled "Protection of Automatically Started Fractional Horsepower Motors," published in the September 1940 issue of the *News-Bulletin*, of the International Association of Electrical Inspectors, shows that only with a properly built-in protector can there be protection and at the same time approach maximum motor output under any possible load condition.

Many aircraft electric motors have intermittent-duty cycles and in order to conserve weight the motors are operated at high speeds, and these intermittent-duty motors are regularly operated at high overloads as compared to a continuous running rating. This results in motors of small size which presents a space limitation for the location of the protector.

Location of Protector

It takes no data or lengthy discussion to convince one that theoretically the proper location for the protector would be at the danger hot spot where it would be responsive to the danger hot-spot temperature. Because of physical limitations, this is impossible; so, there must be a compromise by preparing a location when designing the motor body that will give the thermostatic element the best possible opportunity of absorbing heat from the danger hot spot.

Temperature at Which to Protect

Regardless of the kind of insulation used, insulation life is dependent on both time and temperature. This is best illustrated by the life-temperature curve for class *A* insulation, Figure 1 of this paper, copied from page 434 of the book "Insulation of Electrical Apparatus," by Douglas Miner.

Class *A* insulation is listed by AIEE as good for a maximum safe operating temperature of 105 degrees centigrade. From the curve, it can be seen that this will give a minimum expected life of 2,000 days. The tentative Army-Navy (AN-M-10) electric motor specifications require that the protectors provide a minimum life of five continuous hours of operation under locked rotor conditions without damage to the motor. To provide sufficient manufacturing tolerance, it would be desirable to select a temperature corresponding to a minimum life of five days. This gives a temperature of 178 degrees centigrade.

Even with the selection of this high maximum allowable operating temperature for five days' life of the insulation, it should be remembered that in normal applications the actual operating temperature of the motor will be far below this value. This is best illustrated by Figure 2.

Line *A* represents room ambient of 25 degrees centigrade.

Line *B* represents the maximum winding operating temperature of 105 degrees centigrade under normal loads and 25 degrees centigrade ambient temperatures.

Line *D* represents the maximum winding temperature at which there is a tendency for most engineers to protect the motor. If this temperature of 120 degrees centigrade were selected, it is obvious that a small overload or an abnormally high ambient would cause nuisance tripping of the protector as there is very little winding temperature difference between full load and the point where the protector would open.

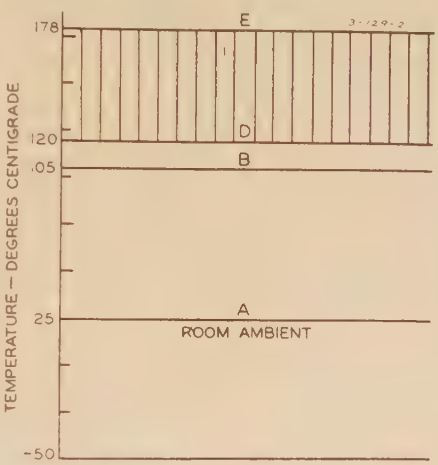


Figure 2. Chart of temperature levels involved in aircraft electric-motor protection

Line *E* is the 178 degree centigrade temperature selected for a minimum of five days' life from Figure 1.

To get protection without limiting the work output of the motor, the protector must control the motor winding temperature at 178 degrees centigrade, line *E*. The shaded area between the curves *D* and *E* represents the thermal capacity which most motor designers would not use. Thus, if protection were applied on the basis of line *D*, representing maximum winding temperature, it would lead to the condition where the aircraft designers (and others) would know that there was additional capacity available in the motors as indicated by the shaded area, and he (or they) would then rather have the operator run the risk of burning out the motor than be limited in power output as indicated by line *D*. The values used in this paper are on the basis that the winding temperature is the limiting hot spot; other hot spots would have different hot-spot maximum temperatures.

The Danger Hot-Spot Temperature

The temperature that will first cause damage to an aircraft electric motor need not be the winding temperature as indicated heretofore, or even the highest temperature in the motor. The first damage may be at the brushes, the solder, or the shunt leads to the brush-holder, and so forth. It is necessary to determine where the danger hot spot is and how hot it gets. This may be estimated at the time of designing, but the actual data can be obtained only by test with thermocouples and other temperature-measuring devices located at every point of possible damage.

Figure 3 is a photograph of such a test setup with eight thermocouples and four voltage-drop leads mounted in the motor. To be complete, these data should be obtained not only at room ambient but also at the maximum and minimum ambients at which the motor is expected to operate, and with various loads from the maximum continuous load the motor can carry without exceeding the maximum allowable hot-spot temperature through and including locked rotor. This is necessary because, for example, under locked rotor the armature windings may be the danger spot, and during running the danger spot may be shifted to the solder on the commutator.

Thermal-Element Operating Temperature

We know that the heat dissipation, or in other words the capacity of a motor, will vary with ambient. The accurate calculation of the capacity at different ambients would involve the use of a number of empirical values based on test data which are not available today. As the major portion of the heat is generated in the copper windings, a simple method of indicating the trend would be to use the following formula:

$$R_x = R_n\sqrt{\frac{T_m - T_x}{T_m - T_n}}$$

R_x is the current rating at a given ambient.
R_n is the current rating at room temperature (25 degrees centigrade).
T_m is the maximum allowable temperature (178 degrees centigrade).
T_n is the room temperature (25 degrees centigrade).
T_x is the given ambient temperature.

Figure 3. Aircraft electric motor equipped with thermocouples and resistance leads

The maximum allowable winding temperature previously adopted in this paper was 178 degrees centigrade. It is assumed that an intermittent-duty motor when carrying 80 per cent of its rating continuously at normal room ambient (25 degrees centigrade) will have a temperature rise of 153 degrees centigrade, resulting in an operating temperature of 178 degrees centigrade.

The curve in Figure 4 is obtained by using these values in the formula. It has been assumed that this intermittent-duty motor when running continuously would only carry 80 per cent of its rating at 25 degrees centigrade ambient. It would carry more load at lower ambients as indicated by the curve at −50 degrees centigrade, where it will carry 97 per cent of its rating continuously. At high ambients the motor output would be limited below the 80 per cent. At 70 degrees centigrade ambient the curve shows the motor will operate continuously at 67.5 per cent of rating. At an ambient of 178 degrees centigrade, the motor should not operate at all because the hot spot is already up to the maximum allowable temperature.

The information in this curve may be stated as follows: At any ambient there is one value of continuous load current that will bring the danger hot-spot temperature up to the maximum allowable value of 178 degrees centigrade (line *E*). If the protector is going to allow the motor to attain this temperature (178 degrees centigrade) at all ambients before removing the motor from the line, a protector will be needed that will have the same ambient characteristics as the motor it is to protect. As the operating temperature of the thermal element must be a fixed value, and if it is to match curve Figure 4, the thermal element must operate at 178 degrees centigrade when carrying zero load. This then establishes the operating temperature of the thermal element at 178 degrees centigrade. It can be stated that theoretically for continuous running loads the operating temperature of the thermal element of the protector should be the same temperature as the temperature of the hot spot to be protected.

Function of the Heat Generated in the Protector

When discussing the location of the protector, it was stated that it was physically impossible to locate the protector at the hot spot. With the protector located in a compromise position, it cannot be expected that the protector will be heated by the temperature of the danger hot spot up to the hot-spot temperature. This is illustrated graphically in Figure 5.

Curve *M*, the ambient temperature versus motor continuous carrying capacity, is the calculated curve from Figure 4, and with a protector mounted in this motor, the curve of the protector temperature, that is, the temperature of the metal parts of the motor which surround the protector, would fall between curve *M* and curve *E*. The exact location of curve *P* would depend on how sensitive the protector was to the hot-spot temperature it is to protect. As the protector is a part of the motor assembly, the shape of the protector temperature curve *P* will be similar to curve *M*. But, regardless of where the protector is mounted, the protector location temperature under continuous operation would be greater than the ambient temperature curve *M* and less than the operating temperature of the thermal element curve *E*.

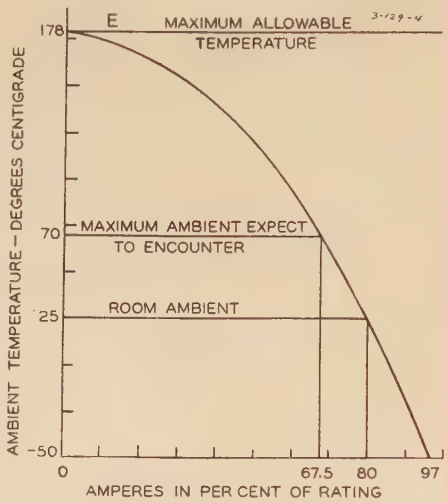


Figure 4. Ambient versus per cent load curve

In order to use a thermal element operating temperature of 178 degrees centigrade, some method must be used to raise the thermal element temperature from that temperature surrounding the protector, curve *P*, up to 178 degrees centigrade. This can be accomplished by generating heat in the protector. Figure 6 is a schematic diagram of a protector with a heater built in for this purpose. This heater is connected so that the load current passes through it and the thermal element, and the heat generated in the protector is proportional to the current flowing at various loads, thus furnishing a variable temperature rise to the thermal element depending on the load current. Therefore, at any ambient there is a per cent of motor load current that will not only cause the hot-spot temperature to reach 178 degrees centigrade, but that same value of current can be used to cause the thermal element to reach the same temperature.

To illustrate, take the point at 70 degrees centigrade ambient. From curve 4, 67.5 per cent rated current will raise the danger hot-spot temperature to 178 degrees centigrade and from curve Figure 5 at this same 70 degrees centigrade ambient, the motor heats the protector to 125 degrees centigrade and the heat generated in the protector must raise the thermal element up to 178 degrees centigrade. Therefore, it can be seen that the horizontal crosshatched area indicates the temperature rise imparted to the protector by the heat of the motor and the vertical crosshatch indicates the temperature rise that must be imparted to the thermal element by the heater.

Ultimate Trip

So far, continuous performance of the motor has been discussed, and, if, after running sufficiently long to reach maximum stable conditions for any given

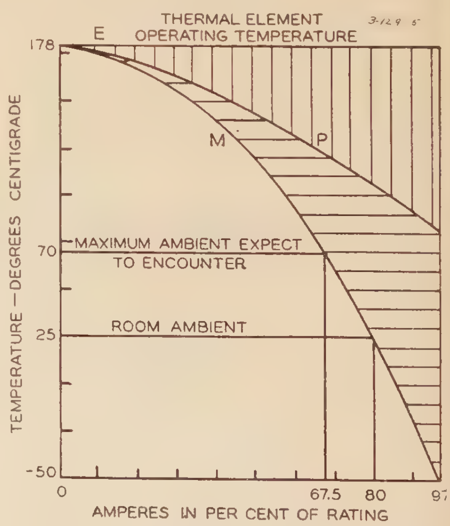


Figure 5. Motor ambient and protector temperature curves

ambient, there would be some change in these stable conditions, such as a rise in the ambient temperature or a slight increase in load current, both the hot-spot and the thermal-element temperature would rise, but as the protector would operate at any temperature in excess of 178 degrees centigrade, the motor would have reached an *ultimate* running condition as the protector would remove the motor from the line. This is called *ultimate trip* and may be stated as that value of load current which at a given ambient will cause tripping of the protector when the motor has reached a stable condition.

Locked Rotor

Locked rotor is the most severe load condition that can be imposed on the motor. Therefore, it is necessary that the protector take care of this condition in a satisfactory manner. For continuous running, the temperature of the protector would not follow the hot-spot temperature. So, under locked rotor, the protector temperature will lag still further behind the hot-spot temperature. This is illustrated in Figure 7.

F is the time-temperature curve of the danger hot spot for locked rotor. Since the locked-rotor current is high, the temperature will rise so rapidly that the protector (curve *G*) absorbs very little temperature from the motor before the hot spot has reached a dangerous temperature. This means that when the rotor is locked, the heat generated in the protector must furnish all of the temperature rise to the thermal element to bring it up to 178 degrees centigrade. The shaded area of Figure 7 shows this temperature rise that must be furnished by heat generated in the protector for 25 degrees centigrade ambient.

Some engineers think that locked-rotor protection is all that is needed for d-c aircraft electric motors. This thinking is based on the fact that the duty cycle of most aircraft electric motors is very short, and the major troubles come from jammed mechanisms which cause stalling of the rotor. Then, as soon as the rotor stops, it heats rapidly as shown in curve *F* of Figure 7. With a constant voltage supply, the current falls off because of the increased resistance of the windings, and the motor starts losing torque.

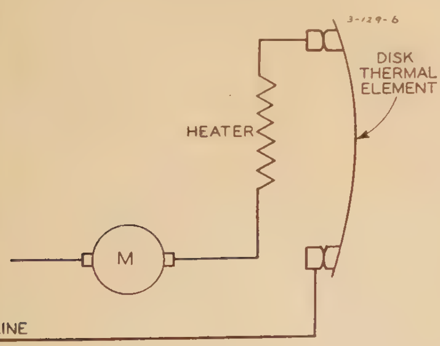


Figure 6. Schematic diagram of heater-type motor protector

If protecting the motor from destructive temperatures was all there was to consider, a protector that provided locked-rotor protection would be a satisfactory protector, and holding the motor on the line for only one second would be sufficient time to let the motor start if free to do so. In aircraft there are many times when mechanisms are not jammed but are damaged sufficiently to introduce loads approaching blocked conditions, and at the same time the motor is able to turn over slowly. While it may take a relatively long time for it to complete its work, the chances are that the motor will do some good if it can be kept operating without burning out. This means that

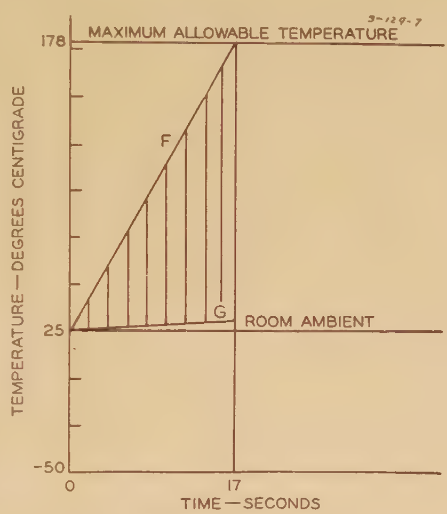


Figure 7. Heating curve for locked rotor

for locked-rotor conditions, the motor should not be taken off the line until the danger hot spot has reached its maximum allowable temperature. In providing locked-rotor protection without regard to the maximum output, it is possible either to overprotect, so as to cause nuisance trip-outs, or to underprotect so that the motor will burn out at normal loads or running overloads.

Overprotection is illustrated in Figure 8. Here the motor is carrying an excessive load but has not reached a stalled-rotor condition before the danger hot spot reached the maximum allowable temperature. The curve *J* is the time-temperature curve under a high running overload, and *K* is the time-temperature curve of the protector location. If this protector were one that took the motor

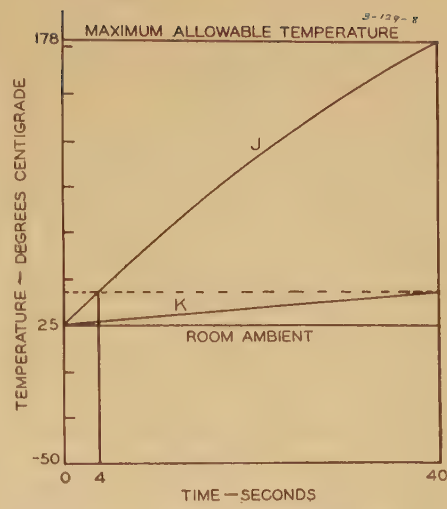


Figure 8. Heating curve at high overloads

with locked rotor off the line in one second, then under this extreme running condition the protector would have taken the motor off the line in probably four seconds, and the motor output would be limited to a small amount of work, and the danger hot spot would have been only 35 degrees centigrade. This kind of locked-rotor protection would not provide the maximum possible output of the motor. By proper design of the protector as to the heat generated in the thermal element, the operating temperature of the heater, the amount of heat, and its location with respect to the thermal element, it is possible to have some control over the rate of temperature rise of the thermal element so that regardless of the load conditions the protector will let the danger hot spot approach its maximum allowable temperature before disconnecting the motor from the line. Under these conditions, the motor can give maximum work output with protection from destructive temperatures regardless of load current or ambient.

Loads Less Than Maximum Continuous Running

Any load below the continuous value that will not heat the danger hot-spot temperature up to its maximum allowable temperature has not been considered in this paper, for, obviously, no protection would be needed for this condition.

Intermittent Loads

Up to this point, the two extremes of load that would call for protector operation to protect the winding hot spot have been discussed:

1. The maximum continuous running load.
2. Locked rotor.

Any value of intermittent load that would call for protector operation in protecting the winding hot spot would fall between these two extremes and need not be given any special consideration.

Regardless of the ambient conditions or load above maximum continuous running, it would be expected that a motor with an inherent overheat protector designed and applied according to the principles laid down in this paper would give a constant maximum danger hot-spot temperature as illustrated in Figure 9.

Curve N is the temperature of the danger hot spot for any value of overload or locked rotor and shows the same hot-spot temperature for all ambients.

In following out these principles, protector and motor designers are confronted with limitations, such as

1. Applying protectors to existing motors.
2. Limitations as to materials for the insulation of the protector.
3. Space limitations.
4. Physical properties of materials for the thermal elements and for the heaters.
5. Manufacturing tolerance more particularly of the motor than the protector.

In view of these limitations, some deviation would be expected from the theoretical curve N in Figure 9.

The curves in Figure 10 (results of tests on a protected aircraft electric motor) show how surprisingly close a protected motor can come to the theoretical conditions when a protector is designed and applied in line with the principles of this paper.

Conclusions

In conclusion, these principles can be summarized as follows:

1. Prepare a protector location when designing the motor.
2. Select those points in the motor whi h are likely to be the danger spots and assign to each such point the maximum temperature possible.
3. Locate the danger hot spot by test with thermocouples and resistance rise measurements.
4. Adopt a thermal-element temperature as close as possible to the protection temperature of the hot spot to be protected.
5. Do not overprotect under any load condition; let tolerance be on the side of a possible loss of a motor now and then rather than rob all motors of a given design of any potential capacity.
6. Supply the difference in temperature between protector location and the thermal-element operating temperature by a heater built into the protector.

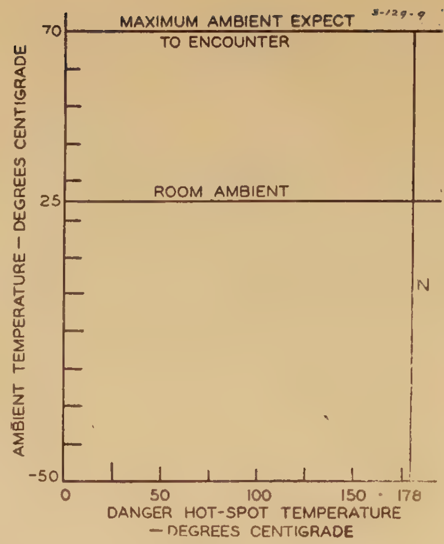


Figure 9. (Theoretical inherent protected motor) ambient versus hot-spot temperature for maximum safe load or overload

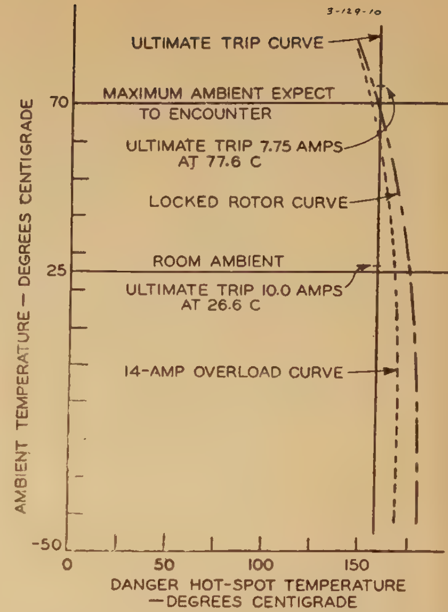


Figure 10. Actual results—inherent protected motor—ambient versus hot-spot temperature for any maximum safe load or overload

7. When using a protected motor, do not undermotor an application.

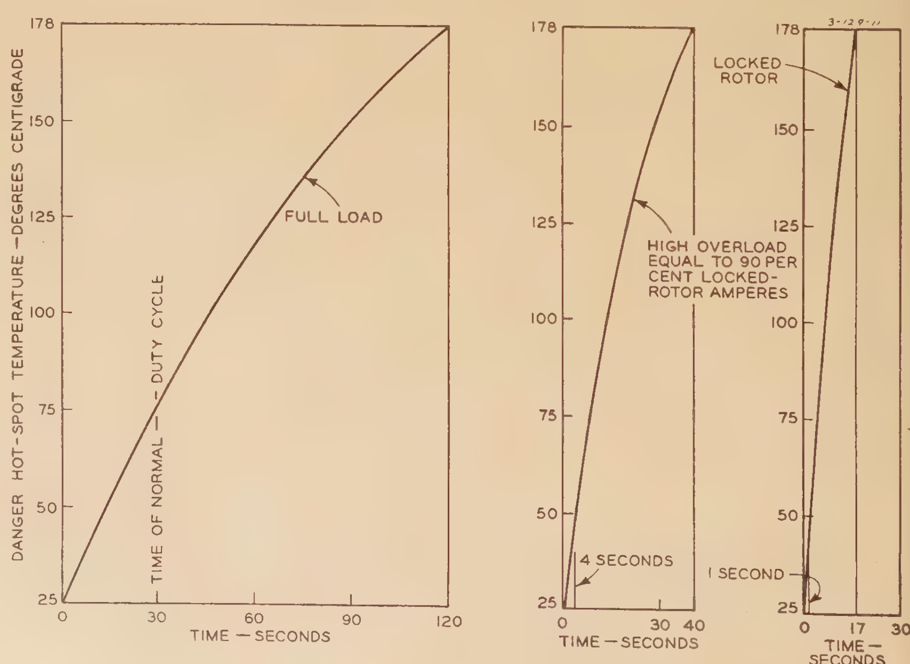
Men's lives depend on the continued functioning of aircraft electric motors. Restricting the output of an electric motor is poor engineering; a burned-out motor is a total loss; protect them so that all allowable output of aircraft electric motors is available to the crews.

Supplement

Since writing this paper, I have had a number of discussions with various people, and as a result of these discussions I have made some theoretical curve sheets which are in this supplement. These curves have particular reference to the results of overprotection and to the results that are likely to occur when one makes an application for locked-rotor protection only.

On curve sheet Figure 11, at the left, is the theoretical time-temperature curve for an intermittent-duty 24-volt d-c motor, run at an ambient of 25 degrees centigrade and with full load current. It is to be noted that the duty cycle is 30 seconds and that it takes 120 seconds for the hot spot to get up to the assumed dangerous temperature of 178 degrees centigrade. From the data on this curve, it is evident that as long as everything

Figure 11. Theoretical time–temperature curves for intermittent-duty 24-volt d-c motor at 25 degrees centigrade ambient temperature



runs normally no protection is required. The motor cannot be overloaded, and it cannot get too hot.

At the extreme right is the theoretical time-temperature curve for locked rotor. Here, we see that the motor will be on the line 17 seconds before the hot spot reaches the assumed dangerous temperature of 178 degrees centigrade.

When a protector is applied to give locked rotor protection only, there is a tendency to set the protector down to as short a time as possible.

In these d-c motors it only takes a fraction of a second for them to come up to speed so that a protector which cuts off at one second would allow ample time for the motor to start and come up to speed, providing there was no excessive load on the motor. A protector with this type of characteristic would give protection for locked rotor, and, if the rotor was definitely locked because of jamming in the mechanism, there is nothing to be gained by leaving the motor on longer than the one second.

In the center curve of Figure 11 we have a theoretical time-temperature curve of this motor when the load is such that the current is approximately 90 per cent of the locked-rotor amperes. Here the motor is running and doing some work, although the speed may be very slow, yet, if kept on the line long enough, the duty cycle could be completed. This would be particularly true if the motor under this running condition were allowed by the protector to run up to its maximum assumed safe temperature of 178 degrees centigrade. In this case, you will note that it would take 40 seconds to attain this temperature. The protector would cut "on" and "off" and the motor would run for a while, and then stand stationary for a while, and then run, and so forth, until the unit had an opportunity to complete the cycle.

On the other hand, if this motor were protected with a protector that cut off at one second under locked conditions, then four seconds would be the maximum that we could expect the protector to let the motor run with 90 per cent of locked amperes, and here we would have such a short "on" cycle that the length of time required for the unit to complete its duty cycle would be so great that it is extremely doubtful if any useful work could be obtained from the motor at this or any other high overloads.

On curve sheet Figure 12, these two types of protector performances are illustrated by the "on" and "off" cycles. In the curve at the top of the page, the protector allowed maximum output of the motor and in the one at the bottom of the page, the protector was applied for locked-rotor protection only (one-second cutoff). By accumulating all of the "on" times, we see that at *A* we have an accumulated running time shown as the result for six cycles of protector operation, whereas at *B* we have the accumulated time for 15 cycles, the 15 cycles having taken place in the same length of time as the six cycles shown.

From experimental work done so far, it appears as if there is considerable likelihood of obtaining the poor performance indicated by applying thermal protectors for locked-rotor protection only. Therefore, it is believed that one of the most important considerations in connection with aircraft electric-motor protection is to make sure that the protector allows the motor to give its maximum capacity under all conditions of load and ambients.

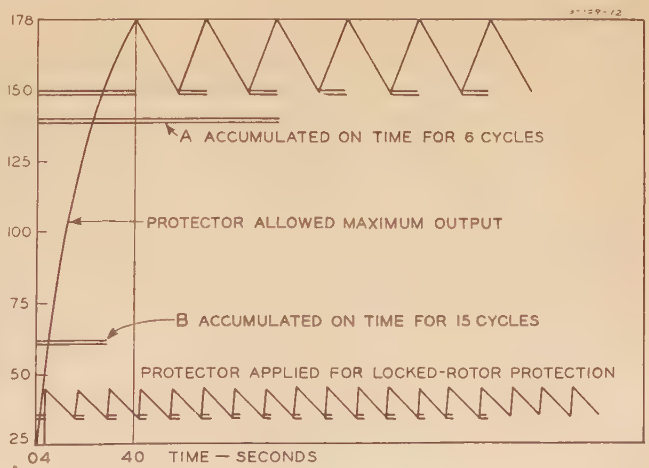


Figure 12. Theoretical time–temperature curves for intermittent-duty 24-volt d-c motor run at 90 per cent locked-rotor amperes

The Testing of Mercury-Arc Rectifiers

H. L. KELLOGG
ASSOCIATE AIEE

C. C. HERSKIND
MEMBER AIEE

THE design of mercury-arc rectifiers is based, as is that of all electrical apparatus, upon knowledge gained from fundamental principles, test data, and operating experience. Our knowledge of the physical action occurring within the rectifier is very limited. While the fundamental processes which take place in an arc discharge have been quite completely described by physicists,[1] their complexity has prevented their expression in a form usable by the engineer. For this reason rectifier design depends, to a larger degree than does the design of most electrical apparatus, upon experimental data obtained from tests.

Of the actions occurring within the rectifier, the phenomenon of arc-back is particularly important as it plays a dominant role in the testing, rating, and performance of mercury-arc rectifiers. Various theories have been proposed in an endeavor to account for arc-backs, and the more recent of these appear to agree quite well with observations and experience.[2,3] Despite these advances, the causes of arc-back are not yet sufficiently well understood so that the effect of specific modifications in the construction may be reliably predicted.

Special testing procedures are required to meet the unique conditions encountered in rectifier operation. Arc-back behavior is a primary consideration in the formulation of load tests involving the determination of arc-back rate. Some correlation has been observed between duty on the rectifier, condition and construction of the rectifier, and frequency of arc-back, but the occurrence of arc-back is essentially random in nature, not only under definite limiting conditions, but under all conditions of rectifier loading. The determination of arc-back rate from load tests is, therefore, a statistical problem. The magnitude of the problem may be appreciated from the fact that one arc-back per month on a six-anode rec-

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H. L. KELLOGG is student engineer, and C. C. HERSKIND is electrical engineer, both with General Electric Company, Schenectady, N. Y.

tifier operating at 60 cycles is but one failure per billion cycles. It is obviously impractical to check such performance by operation under normal conditions, so special testing methods must be devised.[4]

The primary purpose of this paper is to describe the procedure used in testing mercury-arc rectifiers during both development and manufacture. Tentative standards for testing rectifiers are covered by report on AIEE Standard 6 for "Acceptance Tests for Metal-Tank Mercury-Arc Rectifiers," issued in 1934. However, these standards do not cover recent developments in testing technique.

The nature of the electrical and mechanical limitations of the rectifier is discussed in an endeavor to provide a basis for the evaluation of results of tests in terms of expected operation in normal service. The basis for the rating of rectifiers is also discussed and data are presented showing the results of tests on standard rectifiers.

Purpose and Scope of Tests

Tests on mercury-arc rectifiers may be classified under three general heads, depending upon the purpose for which they are made; namely, developmental, commercial, and field tests.

Developmental Tests

Since the fundamental knowledge is not sufficiently complete to provide a satisfactory design basis, new rectifier designs are based upon experience gained from previous designs supplemented whenever necessary by tests bearing directly on the problem at hand. Developmental tests may range from general studies of the physical action to specific tests on a full-size sample of the new design.

Examples of such general studies are: heat transfer and the distribution of losses; vapor pressure, and vapor flow; ionization during the inverse cycle; inverse current to the anode; phase occurrence of arc-back; current distribution during the conduction period; magnetic effects; and so forth. Examples of specific tests are: mechanical, electrical, and thermal measurements on new materials and new structural arrangements for component parts, load tests, control tests, and so forth.

The development of a new rectifier design usually requires the construction of a full-size sample. Developmental tests are made on this sample in order to determine the modifications required to obtain the desired performance. The complexity of the physical action and the lack of complete knowledge of the fundamental processes preclude the use of scale models in making rectifier tests.

Commercial Tests

These are tests made on commercial rectifiers during manufacture for the purpose of

1. Checking the quality of manufacture.
2. Determining the characteristics of the rectifier equipment.
3. Checking the over-all performance.

Field Tests

Despite the steady accumulation of rectifier knowledge and experience, it is still not possible to predict or guarantee in advance the exact performance of a rectifier in the field, particularly as regards the frequency of arc-back. Rectifiers of proved ample capacity and good design may operate with excessive arc-backs when first put into service in new installations, and it is sometimes found necessary to make minor adjustments in the field to obtain proper performance.

Field tests are usually directed at factors not covered by the developmental tests, for example, the effects of longer periods of continuous loading than can be undertaken in the factory tests or different service conditions. Furthermore, field tests must be arranged so that they may be carried on concurrently during normal operation.

List of Tests

A list of the more important tests used in the development and manufacture of mercury-arc rectifiers follows:

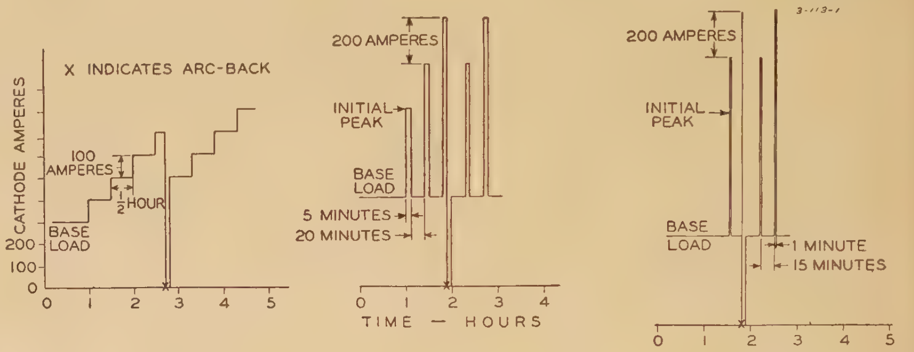
1. Shop tests.
Vacuum tightness.
Degassing.
Dielectric tests.

2. Load tests.
Load-limit tests.
Load runs.
Duty cycle.

3. Short-circuit tests.
Current limit.
D-c short circuit.
Simulated arc-back.

4. Control tests.
Pickup action.
Blocking action.
Phase control.

5. Performance characteristics.
Efficiency.
Voltage regulation.
Power factor.
Wave form.



A. Continuous load limit
1. Start at base load specified
2. Increment, 100 amperes every half-hour, approximately five per cent of basic load
3. Increase load until arc-back occurs
4. Reduce load 200 amperes and continue as before
5. Continue until three successive arc-backs are obtained at substantially equal loads, indicating that limit has been reached

B. Five-minute load limit
1. Start at base load two thirds of continuous load limit
2. Initial peak, 200 amperes greater than continuous load limit
3. Duty cycle—20-minute base load, five-minute peak, repeat
4. Increment, 200 amperes on successive peaks
5. If arc-back occurs, reduce load 200 amperes and continue as before until three arc-backs are obtained at limit

C. One-minute load limit
1. Start at base load two thirds of continuous load limit
2. Initial peak—200 amperes greater than five-minute load limit
3. Duty cycle, 15-minute base load, one-minute peak, repeat
4. Increment, 200 amperes on successive peaks
5. If arc-back occurs, reduce load 200 amperes and continue as before until three arc-backs are obtained at limit

Figure 1. Procedure for load-limit tests

Shop Tests

In the manufacture of pumped rectifiers the first tests made upon the com-

pletion of assembly are vacuum tightness and degassing. These tests provide a check on the basic structural quality and condition the rectifier for full voltage operation. Together with the dielectric tests they comprise the ordinary short commercial tests.

Vacuum-Tightness

The vacuum tightness is checked by taking seepage tests. These tests provide a measure of the tightness of the tank materials, welds, insulating seals, and gasket joints and are usually made both before and after degassing. The test procedure consists in evacuating the rectifier, then shutting off the vacuum pumps with the rectifier at room temperature and noting the increase in pressure over a definite period, usually six hours.

Degassing

While the rectifier degassing may be considered an essential part of the manufacturing process, inasmuch as the rectifier must be conditioned before it will operate at full voltage, the degassing operation also serves as a test of various features of the rectifier. For example, it provides a further check on the soundness of the welds, as the steel parts of the rectifier vacuum tank are heated to operating levels for the first time. A weak weld which appeared tight on the first seepage test may show a leak on the second seepage test. The time required for degassing is a measure of the pumping speed of the vacuum pumps. Some indication of the current capacity of the component parts of the rectifier assembly may be obtained by observing the temperatures of the various parts during degassing. The operation of the cooling system may also be checked during degassing since the rectifier losses at any specified current are substantially the same as during full voltage operation.

Dielectric Tests

High-voltage tests are made in the same manner as for most electrical apparatus using a 60-cycle alternating voltage. Such tests provide a check on the correctness of assembly and the soundness of the solid insulation.

Present AIEE Standards[5] call for the application of high-voltage tests after the rectifier has been thoroughly evacuated and degassed. It is not always possible to make tests under this condition because of the occurrence of breakdowns resulting from glow discharge. The voltage required for breakdown between electrodes in a vacuum may differ considerably from that required for breakdown at atmospheric pressure. Where difficulty of this kind is experienced, it has been the usual practice to make dielectric tests with the rectifier filled with air at atmospheric pressure.

Numerous attempts have been made to correlate the high-voltage break-down characteristics of a rectifier with the occurrence of arc-backs. Tests have been made with an alternating voltage and also with a negative direct voltage on the anode, increasing the voltage until breakdown occurred. Measurements of insulation resistance have also been made. While it is possible to determine the insulation strength and other characteristics of the solid insulation by such tests, they give no indication of the rectifier performance as regards the occurrence of arc-backs.

Load Tests

The purpose of load test on rectifiers is to determine their quality in terms of capacity and reliability. The simple structural abilities are readily evaluated by loading the rectifier and making thermal and mechanical measurements. But the arc-back rate cannot be determined by operating at normal loading because of time limitations. Theory indicates and experience has shown that the performance of a rectifier under normal conditions may be gauged from tests made under more severe conditions of loading.

There are two general types of load tests. One is the accelerated load test, or "load-limit test" as it has been termed, which is used primarily to determine the capacity of the rectifier. The other is the "load-run" or duty-cycle test, which is used for checking the reliability and quality of manufacture. The inherent difference between these tests lies in the method of loading. The procedure in the case of the accelerated test is to increase the loading gradually until failure occurs, either structural or arc-back, while that for the load-run test is to maintain a predetermined loading for a definite period and observe the operation.

The loading or duty on a rectifier involves a number of factors, among which are the following:

1. D-c voltage.
2. Load current.
3. Amount of phase control.
4. Type of circuit and circuit reactance.
5. Duration of loading.

All of these factors must be considered in the application of load tests. However, the usual procedure is to make tests at a given d-c voltage, such as 600 volts, and vary the loading by changing the load current. The amount of phase control may also be varied. Tests are usually limited to a single d-c voltage and one transformer circuit.

A further factor which is usually varied during load tests is the rectifier control temperature as the rectifier performance is a function of its temperature.

Load-Limit Tests

Load-limit tests are made by applying a gradually increasing load on the rectifier until failure occurs, either structurally or because of arc-back. A typical loading procedure which has been used successfully in the development of a number of new rectifiers is shown in Figure 1.

Several factors must be considered in the choice of loading procedure. One of the more important of these is the duration of loading. The thermal storage capacity of a rectifier is small relative to its losses and the thermal time constant is about $^1/_2$ to 1 hour. A one-half-hour interval was, therefore, selected for the continuous test. Shorter intervals were chosen for the five-minute and one-minute tests on the basis that substantially normal temperatures may be obtained by operating at reduced load between peaks. A set of continuous five-minute and one-minute load-limit tests using these time intervals may usually be made in an elapsed time of two or three days.

Another factor is the effect of aging upon rectifier performance. It is well known that the arc-back rate on a rectifier, during initial operation at full voltage immediately following degassing, may be greater than that obtained subsequently after several weeks' or months' operation. This improvement in operation is due to further conditioning occurring under load. This conditioning effect is hastened by operation at heavy loads such as are applied during load-limit tests, and it is, therefore, desirable to obtain several arc-backs at approximately the same load values in order to insure that the load limit has been reached.

It is not always possible to increase the rectifier loading sufficiently to obtain arc-backs. In some types of construction, parts of the rectifier, particularly those in the arc path, may be permanently damaged by excess loading, and this damage may occur before arc-back is obtained. In multianode rectifiers the cathode construction is frequently limiting. However, it has been found desirable, wherever practical, to design the

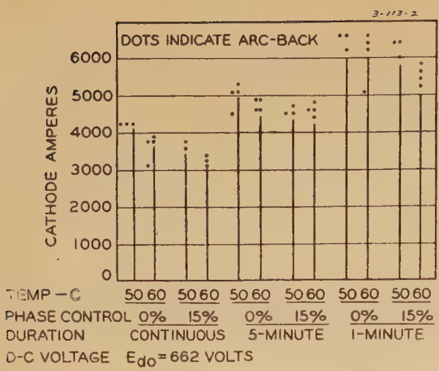


Figure 2. Results of load-limit tests on three ignitron tanks

rectifier so that damage of this kind will not occur, and the load limit is fixed by arc-back. A sturdy construction, capable of operation until arc-back occurs, facilitates testing considerably and permits full attention to the problem of arc-back.

The results of load-limit tests on three ignitron tanks are shown in Figure 2. The standard rating for a rectifier unit consisting of six of these tanks is 2,500 kw at 625 volts. The procedure described in Figure 1 was used in making these tests.

Single anode rectifiers like the ignitron or excitron, may be tested in sets of three since each tank is separate and operates independently. The circuit action, insofar as the voltage and current waves impressed on the anodes are concerned, is substantially the same when three tanks are operating on a single wye as when they are operating as a part of a multiple rectifier, for example, in a double-wye circuit. Three-anode operation facilitates rectifier testing considerably and provides a substantial reduction in time, labor, and power requirements. A large amount of the developmental work on the ignitron has been carried out on three tank units.

The results of load-limit tests on a six-anode multianode rectifier are shown on Figure 3. This rectifier has a standard rating of 1,500 kw at 3,000 volts. Although differing in detail, the procedure used for this test was similar to that already described.

In the case of multianode rectifiers, it is necessary to load the complete rectifier, inasmuch as the cathode and tank are common to all the anodes, and the general physical action in the tank is a function of the cathode current. Also, the ionization attributable to current in one anode affects the neighboring anodes. However, a reduction in the power capacity required for testing a multianode rectifier may be effected by passing current through some of the anodes at low voltage on degassing connection.

The effect of temperature upon rectifier capacity is clearly shown by the load-limit tests for the multianode rectifier, Figure 3. These curves indicate that there is an optimum temperature at which the rectifier capacity is a maximum and that the capacity decreases at higher or lower temperatures. Research[6] has shown that in the lower part of the temperature range the rectifier capacity is defined by a "current limitation" and the maximum current which the rectifier can carry is that which utilizes all the molecules in the mercury vapor as positive ions to neutralize the space charge. When this current is exceeded, the arc drop increases abruptly and arc-back ensues. In the upper part of the temperature range arc-backs occur as a result of voltage breakdown because of high mercury-vapor pressure. The voltage which the rectifier will withstand without arc-back decreases as the vapor pressure increases with rise in temperature. At the higher temperature the rectifier capacity is defined by a "voltage limitation." It is generally desirable to operate a rectifier at as high a temperature as possible without incurring arc-backs.

Load Runs

Load runs are made by applying a predetermined loading for a definite period and observing the operation, for example, counting the number of arc-backs which occur. Such runs are usually made with a constant loading applied for a period of 12 or 24 hours. Load runs at rated load or at loadings in excess of rating provide a check on rectifier performance under conditions approximating those encountered in service. While the period of loading is too short to determine the arc-back frequency on a successful rectifier and the service conditions may not be fully duplicated, such tests do indicate the maximum arc-back rate likely to be incurred in service.

In cases where load-limit tests cannot be applied because of structural or thermal limitations, lack of testing facilities, and so forth, the rectifier capacity may be determined by means of load runs. The procedure consists in making a series of load runs, increasing the loading on successive runs and determining the arc-back rate for each.

Commercial tests on rectifiers include load runs when complete tests are required. They may be made either with the transformer being furnished for installation with the rectifier, or with a test transformer. However, such tests are costly and furthermore do not assure successful operation as regards arc-backs.

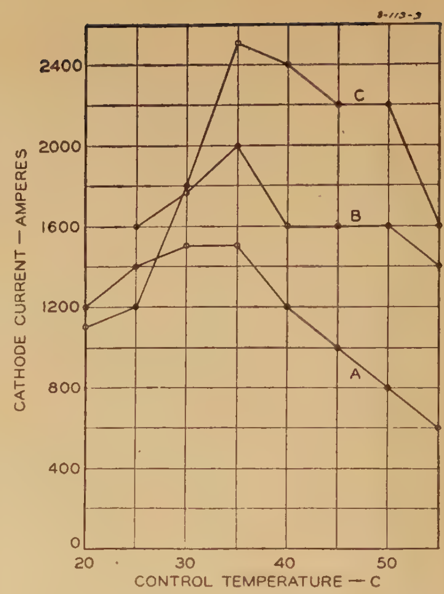


Figure 3. Results of load-limit tests on 3,000-volt multianode rectifier

Curve *A*. Continuous load limit with current increased 100 amperes every two hours

Curve *B*. 20-minute load-limit with current increased 200 amperes on successive trials; 40-minute operation at 500 amperes between peaks

Curve *C*. Five-minute load limit with current increased 200 amperes on successive trials; 25-minute operation at 500 amperes between peaks

For this reason, when a rectifier of a proved design or a large number of rectifiers of the same design are furnished and only short commercial tests are required, load runs may safely be omitted. Often load runs are made on a sampling basis, for instance, by testing every tenth rectifier.

Duty Cycles

The operating conditions incurred in electrochemical service are essentially the same as those obtained during load runs. In cases where the rectifier will operate under variable loads (as for example, railway or steel mill service), it is frequently desirable to make tests, applying a duty cycle simulating the actual operating conditions. This is particularly true where service conditions depart considerably from normal.

Faulty operation may be encountered in rectifiers at light loads as well as under overload conditions. One example of such trouble is mercury condensation on the rectifier anodes at low ambient temperatures with exposure to a cold air flow. Such condensation causes arc-backs on starting. Failures may also arise from the varying temperatures with changing load, which result in mechanical failure attributable to the movement of parts

during expansion and contraction. Another difficulty which may be encountered is failure to obtain stable or constant temperature control with varying loads. Duty cycles simulating the conditions obtained in service provide a further check on the rectifier operation prior to installation, and assist in the elimination of such minor difficulties.

Determination of Rectifier Capacity

Many types of electrical apparatus are designed to meet definite temperature rise limits, for example, a 40 degree centigrade rise.[7] A principal determining factor in establishing temperature rise limits is the thermal endurance of the insulating materials, although other factors must also be considered. Since the physical deterioration of insulation under the influence of time and temperature increases very rapidly with temperature, the choice of temperature rise limit bears directly upon the probable life and reliability of the apparatus. The concept of temperature rise as a basis for rating is a convenient and useful aid in the design of most electrical apparatus.

In the case of mercury-arc rectifiers the construction is usually such that time and temperature produce no appreciable deterioration and the life is so long that replacement will probably not be required before obsolescence occurs. The principal economic considerations are operating cost and reliability. These factors cannot be gauged from any measurement of temperature rise. However, the reliability may be expressed directly in terms of the frequency of arc-back while the cost factors, such as rectifier power losses and maintenance, are also a function of the arc-back rate. Thus, it follows that rectifiers should be rated on a basis of arc-back frequency.

A high arc-back rate results in increased maintenance on protective switchgear, increased strain on transformer insulation, added hazards to operation, and interruption in service. The allowable arc-back rate depends upon the class of service. Many rectifier applications are of such character that an arc-back frequency of the order of one per month is acceptable, and 10 to 30 times this frequency for short periods does not exact any considerable economic penalty. Most railway, electrochemical, and industrial installations fall in this class. There are other applications where continuity of service is so important that an arc-back rate of more than one per year is not acceptable. Public utility installation involving essential service and industrial installations supplying power to gear cutting machines are examples of such applications. The two classes of service appear to necessitate different standards of performance.

Rectifiers having an arc-back rate of the order of one per year or less are usual at the present time for 250-volt service. Experience has shown that such performance is usually obtained when a 600-volt rectifier is operated at 250 volts. While it is possible to build rectifiers for 600-volt service having an arc-back rate not exceeding one per year, it appears that there would be certain economic penalties since the construction will entail considerable improvement in baffling with resultant arc losses, reduction in rating for a given size, and increased complexity of control.

An important consideration in the operation of a rectifier is its stability as regards arc-back. It must be recognized that an acceptable arc-back rate is not always obtained when the rectifier is first put into service. When trouble is experienced on a rectifier installation because of high arc-back frequency, the question is immediately raised, will there be progressive deterioration with an increase in arc-back frequency until the equipment becomes inoperative? Experience to date indicates that while the arc-back frequency may exceed the acceptable or desired value resulting from a large variety of causes, stable operation is generally obtained at a definite arc-back rate.

Short-Circuit Tests

Knowledge of the short-time current capacity of a rectifier is essential to the proper application of protective switchgear, and the choice of system reactance to limit fault currents. Short-circuit tests are of three general types: namely, single-cycle current limit, d-c short circuit, and simulated arc-back tests.

Current-Limit Tests

Basic data regarding the rectifier fault current capacity may be obtained by passing a single cycle of current through one anode with the d-c circuit shorted and increasing the magnitude of this current on successive trials until failure occurs. The behavior of the rectifier under these conditions is determined from oscillographic records.

The specific procedure for making current-limit tests is as follows: A single anode of the rectifier is connected to one phase of the rectifier transformer with the d-c leads short-circuited. The usual protective switchgear in a-c and d-c circuits may be provided but is arranged for delayed operation. The transformer is energized, but conduction is prevented either by negative grid voltage in the case of the multianode rectifier or by absence of ignitor excitation in the case of the ignitron. A single cycle or fault current may be caused to flow by energizing the grid or ignitor for one cycle by means of a welder control. The magnitude of the fault current is controlled by varying the phase of firing or by placing a reactance in series with the transformer circuit. In making these tests, the magnitude of fault current is gradually increased until arc-back occurs. Tests are usually made at several temperatures. Tests may also be made with more than one cycle of fault current.

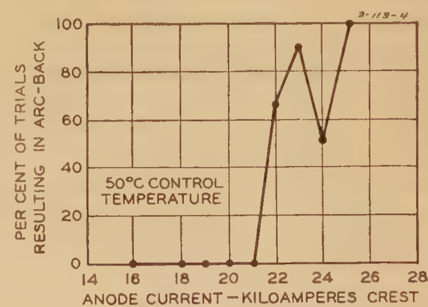


Figure 4. Results of current-limit tests on ignitron rectifier

The results of current-limit tests on a standard ignitron tank are shown in Figure 4, while Figure 5 is a typical oscillogram of a successful single cycle current-limit test.

Failure of control may occur in the course of current-limit tests as well as arc-backs. Control failures may be of various types. For example, in the case of ignitron rectifiers, the anode may fire during the cycle succeeding the conducting cycle without the ignitor being re-energized. In multianode rectifiers where voltage is applied to all the anodes but only one anode is permitted to fire, the other anodes may also fire because of failure of their grids to block. The control characteristics will be more fully described in the section on control tests.

D-C Short Circuit

D-c short-circuit tests are made for the purpose of checking

1. The ability of the rectifier to withstand the fault currents.

2. The operation of the protective switchgear.

The testing procedure consists in connecting a definite low resistance across the

d-c leads of the rectifier by means of a contactor or high capacity switch. The resulting short-circuit current may be interrupted by the rectifier cathode breaker, anode breaker, or the oil circuit breaker in the transformer primary circuit. Or alternatively, the current flow may be suppressed by action of control grids or ignitors. The magnitude and duration of the short-circuit current as well as the operation of the protective switchgear may be checked by oscillograph.

The high currents flowing through the rectifier under short-circuit conditions cause the liberation of gas from the anode and other parts in the arc path. A measure of the speed of the vacuum pumps may be obtained by noting the time required to remove the gases liberated dur-

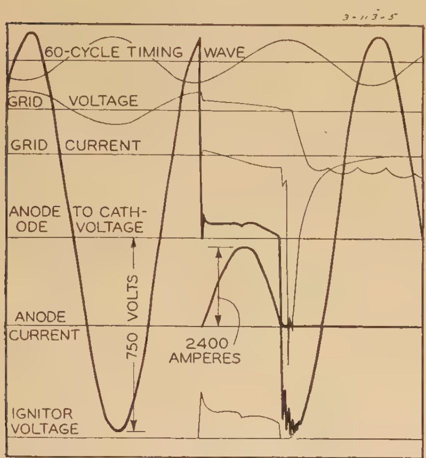


Figure 5. Typical oscillogram of successful single-cycle current-limit test (with negative voltage applied to grid to block firing on succeeding cycle)

ing the short circuit. Short-circuit tests also provide a check on the sturdiness of the rectifier tank and the external bus structure and their ability to withstand short-circuit stresses. While the rectifier must be able to withstand repeated short circuit without damage, it is not essential that it be able to operate without arc-back under the most severe conditions.

Simulated Arc-Back

Since the switchgear furnished with rectifiers is specially selected to provide protection during arc-back it is often desirable to check its effectiveness by direct tests. This may be done by applying a short circuit between one anode of the rectifier and its cathode by means of a contactor or other shorting device, while the rectifier is operating under load. Such a short circuit simulates the conditions obtained when one anode of the rectifier arcs back. The resulting operation of the rectifier and its protective equipment is determined by taking oscillographic records.

Simulated arc-back tests may be made with the rectifier operating on either a water-box load or on a live load such as a motor generator set. In the latter case the load will feed current into the fault. The same is true where two or more rectifiers operate in parallel. Simulated arc-back tests are usually made only where complete tests are required. Because of the limitations of factory facilities, such tests are frequently made in the field.

Control Tests

The starting of anode conduction in a rectifier is controlled by the auxiliary electrodes, such as starting and excitation anodes, grids, and ignitors. In general, these may be arranged to perform the following three functions:

1. To provide the conditions required for anode conduction; namely, a source of electrons (cathode spot) at the cathode, the propagation of ionization in the mercury vapor in the arc path, and a flow of electrons to the anode. These conditions determine the pickup action.
2. To prevent the firing for one or more cycles by exerting a blocking action.
3. To delay the starting of conduction for a definite time each cycle so as to obtain phase control.

Rectifier control requirements vary from those of the simple shunt rectifier without control to those of a fully controlled rectifier with phase control for varying the output voltage and firing control for starting and stopping. The purpose of control tests is to obtain data regarding the various factors entering into the control action, and to check the effectiveness of the control means under the various conditions of operation.

Pickup Tests

Pickup tests are primarily of value in determining the excitation requirements of the rectifier and checking its light load operation. Pickup tests may be made by operating the rectifier at full voltage on a high resistance or counter electromotive force load. The pickup voltage is determined by varying the load resistance or the value of counter electromotive force voltage and observing the d-c volt-ampere characteristics of the rectifier.

In multianode rectifiers the arc is established at the beginning of operation by means of the starting anode and maintained during operation by the excitation anodes which are usually continuously excited. The excitation arc must provide sufficient ionization to assure pickup of each of the main anodes when its voltage becomes positive. When the rectifier is equipped with grids, the grid may also be excited so as to assist anode pickup. In ignitron rectifiers the arc is established during each cycle by means of the ignitor. The ignition current must both initiate the cathode spot and provide sufficient ionization to pick up the anode. As in the case of multianode rectifiers, the pickup characteristics are dependent upon the grid excitation.

The voltage range of the auxiliary power supply is an important consideration in the determination of rectifier excitation requirements. Low voltage is frequently encountered in normal operating service. Sufficient excitation must be provided to obtain satisfactory pickup at the minimum expected auxiliary voltage.

Poor pickup characteristics may result in a variety of troubles. Where several rectifiers operate in parallel and one rectifier is taken out of service, difficulty may be experienced in causing it to pick up and share load with the other rectifiers when it is reconnected to the bus. When rectifiers are installed for automatic operation, the control relay is sometimes arranged so that its action is dependent upon the pickup of the rectifier. In supervisory operation reliable pickup is essential to good operation.

The pickup characteristics of a rectifier may be influenced considerably by the condition of the rectifier tank. For example, it has been found that rectifiers which picked up satisfactorily when new gave trouble as operation was continued and the tanks became freer from gas because of aging. Also, the control temperature and the temperature distribution over the rectifier tank may have an important bearing on the pickup behavior. In multianode rectifiers, poor pickup may result from leakage resistance between rectifier tank and negative ground since the rectifier tank acts as a grid and tends to block the firing of the main anode.

Blocking Tests

Blocking action may be applied under two conditions. In one case it is used to prevent conduction while the rectifier is being placed in service. In the other case it is used to interrupt the rectifying action either at normal loads or under fault conditions. Effective blocking action is more difficult to obtain in the latter case, as the ionization carries over from the conducting cycle.

The minimum blocking voltage required on the grids of a rectifier in order to prevent conduction during starting may be measured by applying a positive d-c voltage to the anodes and a variable negative d-c voltage to the grids. The voltage on the grid is varied until the anode fires. This type of test may be made on either single-anode or multianode rectifiers. A typical grid characteristic for an ignitron is shown on Figure 6.

The effectiveness of the blocking action in interrupting the rectifier current under fault conditions may be determined by means of the same procedure used in making current-limit tests.

Phase-Control Tests

The output voltage of a rectifier may be controlled by varying the phase of the voltage applied to the grids or ignitors. For proper performance of this control function it is essential that a definite relation be accurately maintained between the control voltage applied to grid or ignitor and the anode firing.

There are two possible causes of control failure, namely, loss of control or early firing of the anode, and delay in anode pickup. Some of the factors which may cause control failure are: high load current, high or low temperature, foreign gases, faulty construction. In order to determine the grid or ignitor control characteristics it is necessary to operate the rectifier on load under the various conditions and observe or record the voltage and current characteristics of the control electrode and the anode by means of an oscillograph.

Loss of control is incurred when the anode fires either before or without the application of a control impulse. This may occur in a multianode rectifier when the grid does not prevent ionization reaching the anode and so permits the anode to fire when it becomes positive. In ignitron rectifiers the positive anode voltage acting on the residual ionization may initiate a cathode spot before the ignitor is energized.

A variable time may exist between the firing of the grid and the main anode under certain conditions. This phenomenon has been noted on both multianode and ignitron rectifiers. The difference in time of firing may be reduced to a negligible amount or eliminated by proper choice of firing circuit constants and operating conditions.

The control characteristics of the rectifier unit may be checked by making voltage-regulation tests with specified phase-control settings and comparing

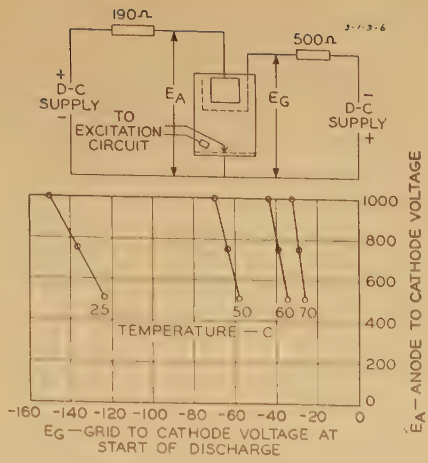


Figure 6. Grid-blocking characteristic of ignitron rectifier

the measured output voltages against the calculated values.

Performance Characteristics

The performance characteristics of a rectifier unit may be determined by direct measurement. However, such tests require the assembly of the complete equipment and present other practical difficulties. The performance characteristics are, therefore, ordinarily determined from calculations based on measurements of rectifier and transformer losses and transformer reactance.

Efficiency

The "directly measured efficiency" is obtained from simultaneous measurements of input and output power. Elaborate precautions must be taken in order to obtain accurate results using this method.[8]

The "conventional efficiency" is obtained from the component losses determined from loss tests. Since most rectifiers have a high efficiency and the losses are only a small part of the total power, a given error in the measurement of the losses does not affect the efficiency as much as the same percentage error in the measurement of total input and output. For this reason the conventional efficiency method is preferred.

The losses incurred in a rectifier equipment are classified as follows:

(*a*). Rectifier arc-drop loss.

(*b*). Rectifier auxiliary losses.

(*c*). Rectifier transformer losses.

Methods for measuring these losses follow:

Arc-Drop Loss

Various methods have been proposed for the measurement of the rectifier arc-drop loss. See the report on AIEE Standard 6.[5] Of these, the oscillographic method has found widest acceptance. However, this method is open to the objection that the rectifier arc losses when operating at low voltage differ from those obtained in full voltage operation since the anode current wave forms are not the same. In order to overcome this objection an improved oscillographic method has been devised which permits operation of the rectifier at full voltage during arc-drop measurements.

The improved oscillographic method employs a noninductive external resistor connected in series with the oscillograph element and a small mercury-arc-rectifier tube (or dry-type rectifier) in shunt with the oscillograph element as shown in Figure 7. The shunting rectifier is connected so as to limit the voltage across the oscillograph element during the inverse period. This voltage may be further reduced by means of a battery connected in series with the shunting rectifier.

A typical arc-drop oscillogram of a large ignitron is shown in Figure 8. This oscillogram shows the arc-drop voltage

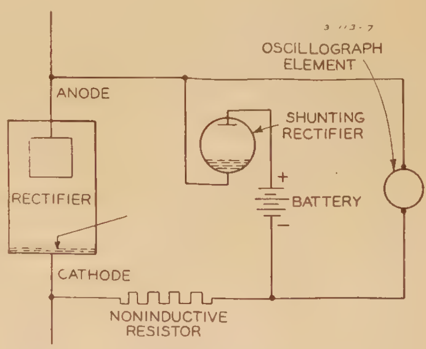


Figure 7. Schematic diagram of oscillograph circuit for measuring rectifier arc drop

and the anode current. The anode current wave is used to determine the limits of the conduction period. On large pumped rectifiers, there is considerable inductive effect in the rectifier tank itself which results in an inductive voltage during the commutation periods. This inductive voltage is superimposed on the actual arc-drop voltage so that the limits of the conduction period cannot be determined directly from the arc-drop voltage wave.

The average arc-drop voltage is obtained from the oscillogram by determining the average ordinate of the segment of the arc-drop voltage wave within the limits of the conduction period by means of a planimeter. The arc-drop loss is equal to the product of this voltage

multiplied by the total cathode current measured simultaneously with the taking of the oscillogram.

On small rectifiers where inductive effects are negligible and the arc-drop voltage wave is essentially flat, the average arc-drop voltage may be measured directly on a d-c voltmeter by using a comparison method. In this method the arc-drop voltage wave is observed on a cathode-ray oscillograph, and the amplification is adjusted so as to obtain a suitable deflection on the screen. The oscillograph element is then switched to a calibrating circuit consisting of a potentiometer with a d-c voltmeter. The oscillograph element is connected to the voltmeter terminals, and the d-c voltage drop is adjusted until the same deflection is obtained on the oscillograph as when viewing the arc-drop voltage.

The oscillographic method of measuring the arc drop in rectifiers has one advantage over other methods, in that it permits observation of the rectifying action during full voltage operation. This is useful in the study of rectifier action in the course of developmental or field tests.

Auxiliary Losses

The auxiliary losses are determined by measuring the input power to the auxiliaries.

Rectifier Transformer Losses

No-Load Loss Tests. Tests for no-load losses may be made in accordance with the standard methods used for other transformers.

Load Loss Tests. The load losses for rectifier transformers may be determined by several methods. One of these is described in the report on AIEE Standard 6.[5] This method requires tests with two or more connections for some circuits. An alternative method which requires only a single connection is described in Appendix A.

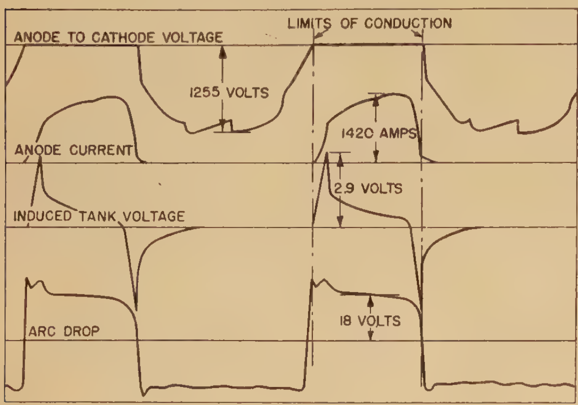


Figure 8. Typical oscillogram of arc drop in ignitron rectifier

Inductive voltage measured by passing anode current through duplicate tank having copper bus connecting anode direct to bottom of tank in mercury pool

Voltage Regulation

As in the case of efficiency, the voltage regulation of a rectifier unit may be determined from direct measurement. The procedure consists in measuring the output voltage at various values of output current with a constant voltage applied to the primary winding of the transformer. Since the reactance of the supply system effects the apparent regulation of the rectifier unit, the regulation cannot be determined accurately by direct measurement. Therefore, the regulation is ordinarily determined from values of output voltage calculated by subtracting the voltage drops in the rectifier and transformer from the rectifier no-load voltage. The method of calculating the output voltage is described in Appendix B.

Power Factor

The power factor of a rectifier unit may be determined from simultaneous measurements of the input power and input volt-amperes. The reactance of the supply system affects the power factor in the same manner as it does the regulation. For this reason direct measurement of power factor under test conditions must be considered approximate. The power factor is generally determined by calculations based on separately measured characteristics of the rectifier and transformer such as the transformer reactance and magnetizing current.

Wave Form

The harmonic composition of the rectifier input and output waves may be determined either by direct harmonic measurements or from analyses of oscillograms. The over-all effect of all the harmonics may also be determined from telephone-influence-factor measurements The wave form of a rectifier is of importance primarily as one of the factors of which a knowledge is desirable in evaluating the influence of the power system on neighboring communication circuits. Since the harmonics produced by a rectifier are affected by the reactance of both the a-c and d-c power circuits to which the rectifier is connected, measurements made under test conditions in the factory may differ from those obtained in the field under actual operating conditions because of the different constants in the two circuits. Methods are available for calculating with reasonable accuracy the wave shape of the voltage and the current in both the a-c and d-c sides of a given rectifier installation when the circuit constants are known.[9]

Testing Facilities

Extensive facilities are required for the testing of mercury-arc rectifiers. There must be provided a complete power system capable of supplying and loading the rectifier to the limit of its capacity, an auxiliary power supply together with the essential rectifier auxiliaries, and an extensive assortment of both standard and special instruments. An arrangement of the rectifier test equipment is shown in Figure 9.

The a-c power system used for testing rectifiers must be of sufficient capacity to maintain substantially sinusoidal wave shape at full rectifier loading and also to give short-circuit currents approximating those obtained in service. Inasmuch as rectifiers must be operated at and above their rated loads for considerable periods during the course of tests, motor generator sets arranged to pump back into the

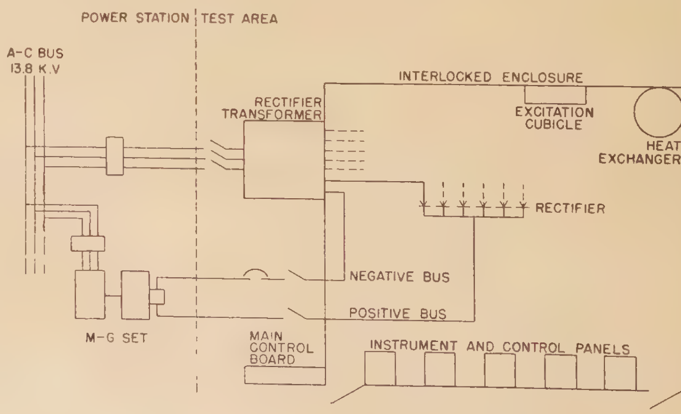


Figure 9. Arrangement of rectifier test equipment

a-c system are used for absorbing the rectifier output.

The generous use of oscillographic instruments is essential in rectifier testing. Oscillographic equipment must include both magnetic and cathode-ray types and must provide facilities for both visual observation and photographic recording. Special instruments and special instrument techniques have been developed for use in rectifier testing. Of these perhaps the most important are the arc-back indicators, the memory oscillograph, and the technique for measuring arc drop. The full use of all available instruments and the development of new instruments is essential to further progress in the analysis of rectifier action.

Appendix A. Transformer Load Loss

The transformer losses may be determined from measurements made by short-circuiting all secondary windings and passing rated sinusoidal current at rated frequency into the primary windings. The losses absorbed by the transformer during this test represent the full-load copper losses when the current is sinusoidal. The total copper losses when operating with non-sinusoidal current in rectifier service may be calculated from this test value as follows:

Symbols

The symbols used in the formulas are defined as follows:

I_p = rated primary current
I_s = rated secondary current

(These primary and secondary currents are the rms values of current for a flat top wave without overlapping.)

R_p = ohmic resistance of primary winding
R_s = ohmic resistance of secondary winding

(These resistances are for copper temperature of 75 degrees centigrade.)

K_p = rated kilovolt-amperes of primary windings
K_s = rated kilovolt-amperes of secondary windings
n = turn ratio
P_1 = losses absorbed by transformer during test
P_2 = ohmic losses for sinusoidal current
P_3 = stray load losses for sinusoidal current
P_4 = ohmic losses for rectangular current
P_5 = total load losses for rectangular current

Calculation of Stray Load Losses

The losses absorbed by the transformer during the test, P_1, should be corrected to a temperature of 75 degrees centigrade.

The current in the short-circuited secondary winding of the transformer during the test will be $I_s' = nI_p$ with rated sinusoidal current of value I_p flowing in the primary winding. The ohmic losses for sinusoidal currents of these values, P_2, are:

$$P_2 = (I_p)^2R_p + (I_s')^2R_s = I_p^2(R_p + n^2R_s)$$

The stray load losses for sinusoidal currents, P_3, are then:

$$P_3 = P_1 - P_2$$

It is assumed that these stray load losses are the same whether the current of the transformer is sinusoidal or has the rectangular wave form obtained during the normal operation of the rectifier. This assumption is justified by a large number of tests carried out by different methods, the variations not being larger with this method than with other methods. With this method, however, it is assumed that the requirements of the various windings of the transformers as to symmetry are entirely fulfilled.

Calculation of Total Load Losses

The ohmic losses for rectangular current, P_4, which are obtained during normal service of the rectifier are:

$$P_4 = I_p^2R_p + I_s^2R_s$$

The total load losses of the transformer during normal service of the rectifier, P_5, are equal to the sum of the ohmic and stray load losses and:

$$P_5 = P_3 + P_4 = P_1 + I_s^2R_s\left[1 - \left(\frac{I_s'}{I_s}\right)^2\right]$$
$$= P_1 + I_s^2R_s\left[1 - \left(\frac{K_p}{K_s}\right)^2\right]$$

When making load-loss tests on the rectifier transformer, the interphase transformers and anode reactors should be disconnected.

For interphase transformers in which the load current is almost entirely direct current, the d-c resistance of the winding may be used in computing the load loss.

Appendix B. Rectifier Output Voltage

The formula for the calculation of the d-c voltage of the rectifier unit at a specified load current is:

$$E_d = E_{d0} - E_x - E_r - E_a$$

where

E_d = d-c voltage at the specified load current
E_{d0} = theoretical d-c voltage (This is the voltage which would be obtained at no-load with no phase control. It is the voltage obtained at the intersection of the regulation curve projected to the zero load line, neglecting the increase in voltage at light load.)
E_x = commutating reactance voltage drop
E_r = resistance voltage drop
E_a = arc voltage drop

The theoretical d-c voltage and the voltage drops may be calculated by using the following formulas:

Theoretical d-c voltage $E_{d0} = \sqrt{2}E_s\frac{P}{\pi}\sin\frac{\pi}{P}$

Commutating reactance voltage drop

$$E_x = \frac{I_cX_cP}{2\pi}$$

Resistance voltage drop $E_r = \frac{W}{I_d}$

in which

E_s = secondary line to neutral voltage of rectifier transformer (rms value)
P = number of phases in simple rectifier
I_c = direct current commutated
X_c = commutating reactance in ohms (line to neutral)
W = load losses of main and interphase transformers
I_d = d-c load current

The commutating reactance X_c, attributable to the main transformer, may be measured by short-circuiting the primary windings and passing sinusoidal current at rated frequency between two secondary anode leads between which a transfer of current occurs during commutation. The commutating reactance anode-to-neutral is obtained by dividing one half of the measured impedance voltage by the applied current.

The transformer load loss W may be determined by the method described in Appendix A.

The method for determining the arc-drop voltage E_a is described in the section on efficiency tests.

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A New-Type Carrier Relay Protection

E. M. WOOD
FELLOW AIEE

H. W. HABERL
ASSOCIATE AIEE

F. A. A. BAILY
ASSOCIATE AIEE

THE Hydro-Electric Power Commission of Ontario and the Beauharnois Light, Heat, and Power Company have placed in service recently a new type of power-line-carrier equipment. Channels for high-class interterminal communication are combined with other channels which provide a transfer of impulses for use with the relaying on transmission lines, giving a desired improvement in the speed of operation.

Stability Problem

The 220-kv 25-cycle system of the Hydro-Electric Power Commission is shown in Figure 1, and has been described in the AIEE Transactions.[1] In the earlier paper a record of experience as to line faults was presented and analyzed. The problem of relaying in relation to transient stability was discussed. It was pointed out that studies on an a-c network calculator and operating experience indicated that, in order to maintain stability at the normal line loadings of 150,000 to 170,000 kw per main line, it would be necessary not only to clear two-phase-to-ground and three-phase faults at the line terminal nearest to the fault in 0.2 second approximately but also to open the breakers at the remote terminal simultaneously or in 0.25 or 0.3 second at the most. If this could be accomplished, transient stability would not be a problem on this system which carries an important industrial load of 600,000 to 650,000 kw in large centers of population.

The original basic scheme of phase relaying on the four main 220-kv trunk lines, as stated in the earlier paper, consists of directioned two-stage impedance relays of a conventional type. The first stage operates in one cycle (0.04 second) or less for all faults within the 80 to 90 per cent of the line adjacent to the relays. The second range, covering the whole line and overlapping into the contiguous line sections, is set to operate in 12 to 20 cycles (0.5 to 0.8 second), thus being time selective with the instantaneous relays on these sections. This basic relaying scheme has proved very successful during the development period of the system when loads were lighter; but with present loadings it will be seen that it does not meet the requirements for stability, because clearance is delayed too long at the remote terminal for faults located within the 10 to 20 per cent section at the end of a line. It will be noted also that the line from Beauharnois to Chats Falls is tapped for the infeed from the MacLaren-Quebec Power Company, thus presenting a problem in distance relaying. Moreover, there are two short line sections which have impedances at 25 cycles that are too low for best results with plain distance relaying.

Regarding the long lines, there was doubt as to the desirability of depending on the transmission of a blocking signal to prevent incorrect tripping. Therefore, a carrier relaying system was proposed which could be applied to any or all types of impedance relays without changing their normal use. Since the carrier relaying feature should be supplementary to the original relaying system, the failure of the carrier link should not in any way affect the normal operation of the distance impedance relays which use time delay to select the faulted line. The carrier feature should provide only a speed-up in the relay action.

When the first stage distance relays are set to operate for faults which are within the 80 to 90 per cent of the line adjacent to the relays, the carrier protection is unnecessary except for faults which are in the 10 to 20 per cent sections at the ends of the line. The latter are seldom more than 20 per cent of the total number of line faults. Therefore, the addition of carrier as a protection or improvement in the protection on any transmission system should be balanced against this 20 per cent of the faults. If the carrier should fail to operate for any cause whatsoever, only 20 per cent of the total system faults would be affected adversely, and in no case would the incorrect operation or failure in the carrier system be detrimental to the relay system because failure only increases the clearing time for faults in the end sections of the line.

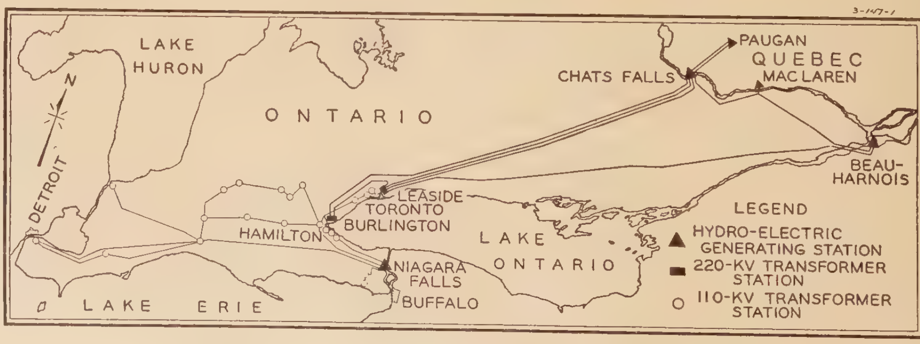
There existed also the problem of communication between the Beauharnois terminal and the commission's plants at Chats Falls and Leaside. Paugan, Chats Falls, Leaside, and stations of the 110-kv system had been interlinked by the commission's overhead telephone lines, but no such provision had been made for MacLaren or Beauharnois. It was desired to link these terminals, particularly the large source at Beauharnois, with the rest of the system by a suitable communication channel which could be of the power-line-carrier type.

Requirements of Carrier System

The relay system should use the directioned instantaneous relay, which protects most of the line, to set up the trip circuit for its own breakers. At the same time this operation would cause an impulse to be sent to the remote end to by-pass the timer on the second impedance range, thereby giving clearances at both ends of the line which are sufficiently near to simultaneous operation to meet the stability requirements. In this scheme, it is not necessary to depend on the passage of the carrier impulse to prevent tripping on external faults; therefore, it would be inherently safer and preferable to the blocking scheme.

The impulse for relaying purposes on one line might be produced by a single frequency or tone in the audio range,

Figure 1. High-voltage lines in the 25-cycle system



Paper 43-147, recommended by the AIEE committee on protective devices and joint subcommittee on power system applications of carrier current for presentation at the AIEE national technical meeting, Salt Lake City, Utah, September 2–4, 1943. Manuscript submitted June 16, 1943; made available for printing July 16, 1943.

E. M. Wood is planning engineer for the Hydro-Electric Power Commission of Ontario, Toronto, Ont., Canada. H. W. Haberl is protection and relay engineer for Montreal Light, Heat, and Power Consolidated, Montreal, Que., Canada. F. A. A. Baily is radio engineer with the Canadian Marconi Company, Montreal, Que., Canada.

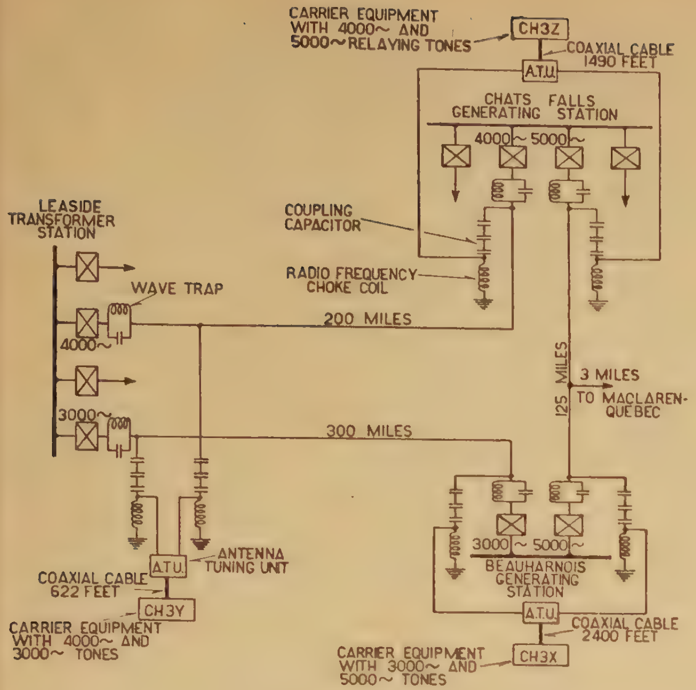


Figure 2. Arrangement of carrier equipment and tones for relaying

which is used for amplitude modulation of the carrier. A separate tone would be assigned to each high-voltage line. One tone generator for transmitting and one tone filter for receiving are required at each end of a protected line, because the impulse for use with the protective relays might originate at either end of the line. An economical design is possible, when only the tone generators and filters which are required are installed at each station. The number of tones which can be used and their place in the frequency spectrum are determined by the sharpness of cut-off in the band-pass filters. Three tones of 3,000, 4,000, and 5,000 cycles were chosen and assigned to the lines, as shown in Figure 2.

The usefulness of this method of carrier relaying depends on whether or not the impulse can be transmitted past the line fault. Tests on lines with grounded conductors showed that, with suitable carrier sets, the impulse could be sent past a ground on one or two wires of the circuit and probably past arcing grounds on all three wires.

Investigation showed that equipment could be obtained wherein both the desired functions of communication and relaying could be combined. Moreover, one set could be coupled to two transmission lines at each terminal, so that one set could be used to serve two lines. It was decided to proceed with a trial installation which would provide communication with Beauharnois and an improvement in the relaying on three lines shown in Figure 2, with the intention that if operation is successful, similar equipment will be installed on the other long lines. The sets and the installation are described in more detail in a later part of this paper.

Auxiliary and Out-of-Step Relays

A schematic diagram of the relay system at one end of the transmission system is shown in Figure 3. A description of the standard relay system for protection of a transmission line is unnecessary. However, the additional relays which are shown in this diagram will be described in the paragraphs hereinafter.

The out-of-step relays are standard "slug" relays of the telephone type, which are equipped with a sealed mercury contact for opening a circuit after a time delay. These operate in the following man-

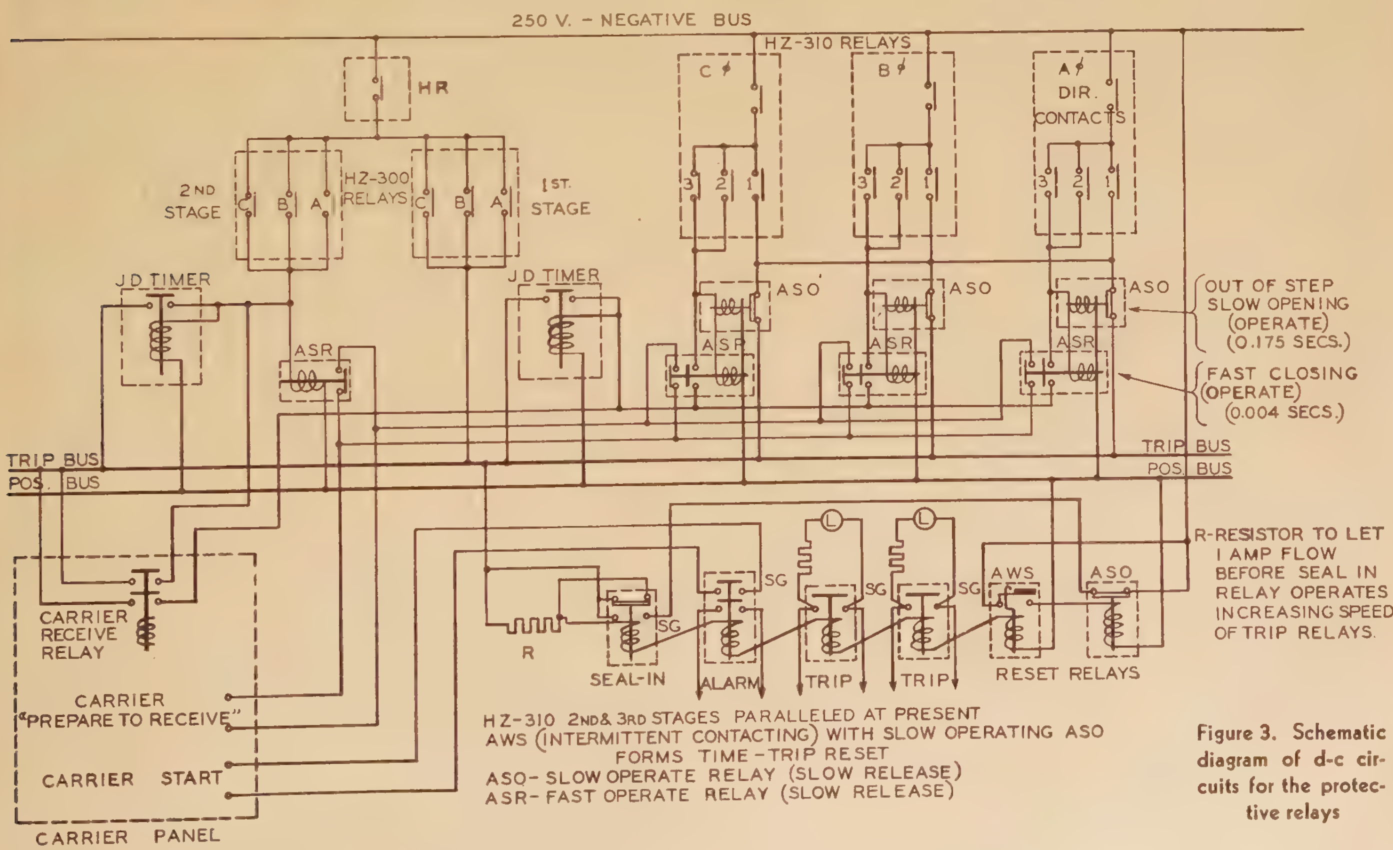


Figure 3. Schematic diagram of d-c circuits for the protective relays

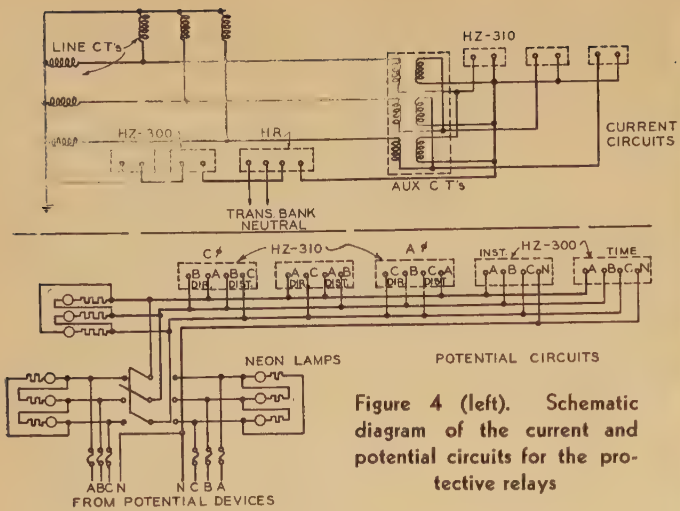


Figure 4 (left). Schematic diagram of the current and potential circuits for the protective relays

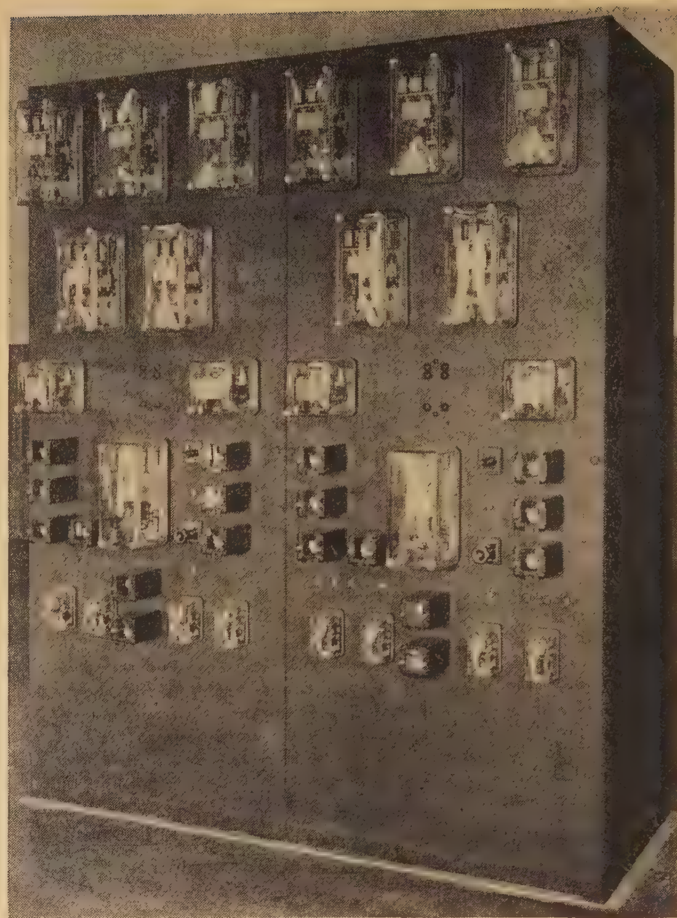


Figure 5. Front view of panel showing the protective relays

ner: If the system should become unstable, all three of the second-stage impedance relays will operate during the swing, and open the trip circuit of the first-stage impedance relays. If the system should remain out of step, the second-stage relays would permit tripping ultimately through the contacts of the *JD* timer. However, if the system should swing back, the first-stage impedance relays will reset, followed by the second-stage impedance relays and the type *ASO* relays, provided the *JD* timer has not closed its contacts. If the out-of-step condition progresses or revolves faster than the time setting of the *ASO* relays, the trip circuit will be closed through the contacts of the first-stage impedance relays. The operation of the first-stage relays on faults within the 80 to 90 per cent section of the line is not affected in any way, as the trip circuit would be closed prior to the operation of the *ASO* relays.

The auxiliary relays type *ASR* are similar to the type *ASO*, except that their action is not delayed. Their function is twofold: first, they operate the *JD* timer, and second, they prepare the carrier set for reception of the tone or impulse, if the carrier equipment happens to be in use for transmitting a telephone conversation at the time.

The type *ASO* and *AWS* relays for resetting the trip circuit are similar to the relays described heretofore, and their function is merely to keep the trip-circuit type *SG* relays in the closed position for a predetermined time, during which the carrier transmitter is started and the appropriate relaying tone is transmitted to the remote station. This time delay must be sufficiently long to trip the local breakers properly and to permit the reception of the relaying tone at the far end of the line where the contacts on the timer for the second-stage impedance relays must be by-passed long enough to permit tripping the breakers.

The current and potential circuits for the protective relays are shown in Figure 4, which contains some points of interest. Delta connection of the neon lamps was necessary because they would not always "strike" on the phase-to-neutral voltage of approximately 60 volts. Star-connected current transformers are used as the source for both the phase-to-neutral impedance relays and the phase-to-phase impedance relays. The zig-zag connection of the auxiliary current transformers has some advantages: first, it allows the use of ground relays while maintaining delta equivalent currents in the phase relays, and second, the volt-ampere burden on the main current transformers is less than straight isolating auxiliary transformers.

Figure 5 shows a general view of the power-line protective-relay panels. At the top of the panels are the phase relays. The ground relays are next, and below these are the timing relays. The type *HR* directioned ground-current relays are in the center of the panels farther down. The type *ASO* relays for out-of-step operation are on the right of the type *HR* relay, and the "prepare-to-receive" relays type *ASR* are at the left. The bottom line of relays are the "trip", "carrier start", "alarm," and "seal-in" relays. The neon lamps are without covers. The telephone-type lamps are used for trip-circuit supervision.

Carrier Equipment

The carrier equipment at each terminal consists of the following units: the carrier transmitter which is connected by a carrier frequency transmission cable to the line tuning unit, the coupling capacitor by which carrier is impressed on each line, the line trap which isolates the carrier currents from the power frequency switching equipment and transformers, and the control unit which includes the carrier receivers.

Transmitter

The transmitter is capable of delivering a maximum of approximately 400 watts of carrier energy, which can be modulated at least 90 per cent, into a load of nominal value of 70 ohms. This is the characteristic impedance of the carrier frequency transmission cable employed to transmit the energy to the line tuning unit and thence to the coupling capacitor and the high-voltage line. The carrier frequency range is 145 to 205 kilocycles. Low temperature-drift quartz crystals in

duplicate are employed as the primary source of carrier energy.

The carrier circuits consist essentially of a crystal oscillator followed by a low-power buffer amplifier, a driver amplifier, an intermediate power amplifier, and a final power amplifier. All circuits are tuned with the exception of the buffer amplifier, which is impedance coupled to the driver. The driver may be connected as a master oscillator by a single switching operation. The output circuits are designed to couple to a transmission cable or other nonreactive load of approximately 70 ohms and to pass the sidebands represented by modulating frequencies up to 6,000 cycles per second without serious attenuation.

The power output of the transmitter may be reduced to a minimum of approximately 15 watts. Power reduction is accomplished in three coarse steps corresponding to approximately 165, 50, and 15 watts. The method used consists essentially of inserting resistance in the plate circuit of the final amplifier stage, thus reducing the net plate voltage. The primary reason for thus reducing the power is to minimize interference on other services, since, under normal conditions and for short distances, the full power is not required for telephony. However, the power must be raised to its full value when a signalling tone is to be transmitted, so a contactor is employed to shunt the resistance out when signalling is required.

The audio-frequency system consists of an input amplifier stage, a driver stage, and a push–pull modulator stage. The system was specially designed to give an audio characteristic which is substantially uniform from 100 to 6,000 cycles. The total harmonic distortion with full carrier power and 90 per cent modulation depth is less than five per cent. The carrier noise is more than 45 decibels below 90 per cent modulation.

Included in the transmitter is a changeover relay actuated primarily by the voice-operated devices and the tone signalling circuits. This relay transfers the output circuits from "received" to "transmit", besides controlling the carrier. The carrier transmission is started by applying plate voltage to one of the lower stages. In the "stand-by" condition, all other plate voltages remain on, and the anode currents are reduced by appropriate grid bias voltages. The crystal stage is allowed to oscillate continuously, as crystals on these carrier frequencies are often slow in starting. By the use of suitable shields and filters, any pickup in the associated receiver is avoided.

Control Unit

The control cabinet includes the following units: two carrier receivers, a receiving amplifier containing the voice-operated disabling device, and an amplifier for the calling loudspeaker, a transmitting amplifier containing the voice-operated carrier-control device and calling oscillator, two low-pass filters, two tone generators, two filter amplifiers, and a calling unit containing the hybrid coil, auxiliary relays, and microphone supply.

Each of the carrier receivers consists of a seven-tube unit, designed for the reception of frequencies between 140 and 200 kilocycles, and comprising a tuned radio-frequency amplifier controlled by an amplified automatic volume control circuit. The input circuits to the receivers include two wave traps and an input attenuator, together with a muting relay which blocks the receiver while the transmitter is operating. This relay is controlled by contacts on the antenna changeover relay, in such a way as to close the contacts on the muting relay before the transmission of carrier begins, and thereby eliminate at the receiver output any pickup from the carrier. This pickup is quite appreciable and is due to a large extent to the capacity effects at the antenna changeover relay. The wave traps are incorporated to provide mitigation of adjacent channel interference. The input attenuator provides a coarse adjustment of carrier input level to ensure that the normal incoming carrier will work the *AVC* circuits properly and to assist in overcoming the effects of noise. Since the noise level is relatively high, the receiver is relatively insensitive, requiring a minimum input of 45 millivolts

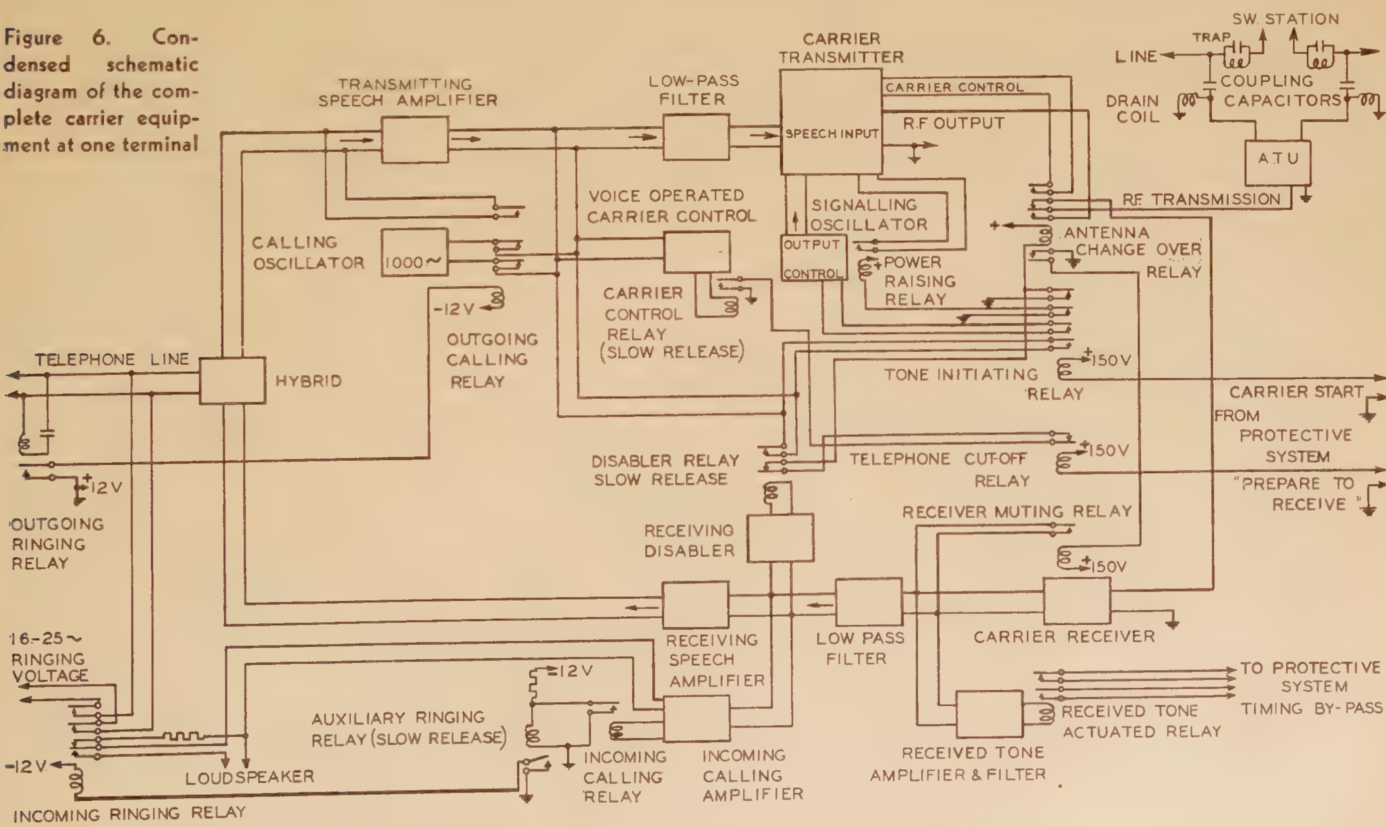


Figure 6. Condensed schematic diagram of the complete carrier equipment at one terminal

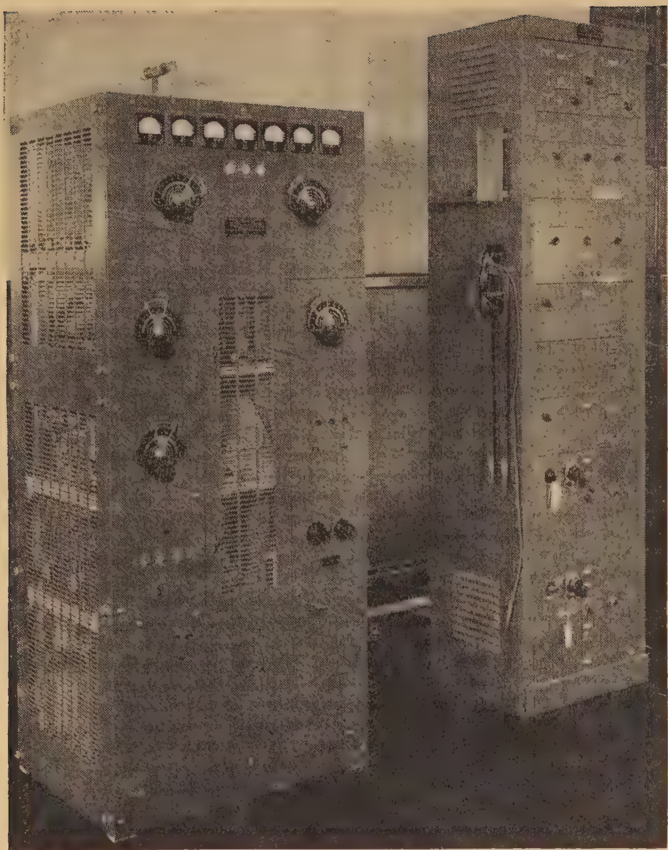


Figure 7. Front view of carrier transmitter and control cabinet

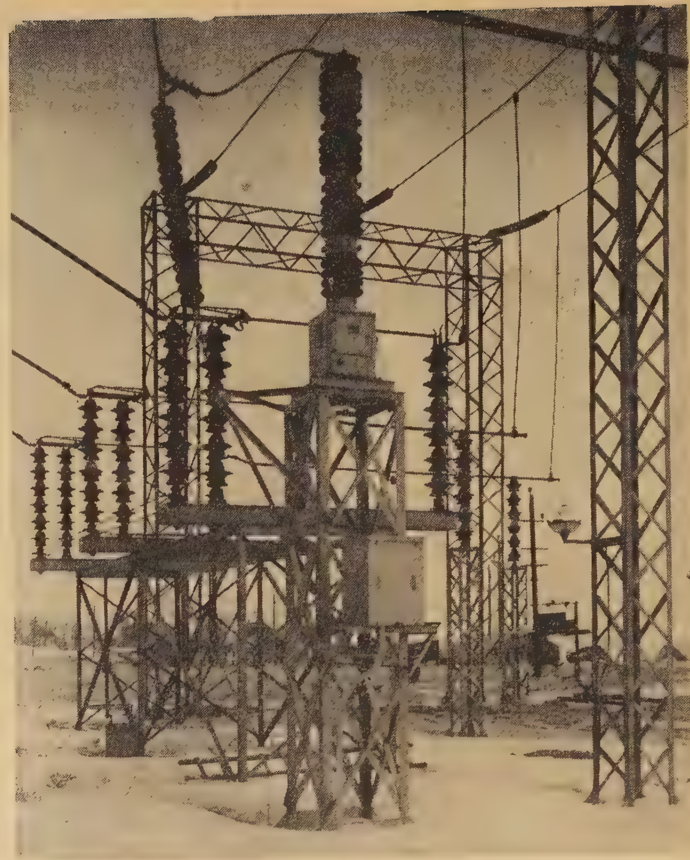


Figure 8. Antenna tuning unit and 220-kv coupling capacitor

for 110 milliwatts ouput. The normal input to the receiver may be as much as one or two volts, depending on transmitter power and attenuation between sending and receiving stations.

The receiving amplifier consists of the following: a single-stage speech amplifier to raise and control the level of the received speech prior to feeding the telephone line, a two-stage amplifier for the calling circuits, and a voice-operated disabling device. The output of the calling amplifier is divided two ways, part operating a conventional loudspeaker, and part being rectified for the operation of the various calling and ringing relays. The disabler includes gas-filled rectifier tubes which are triggered by the incoming speech from the radio frequency receiver, causing the closure of a relay which acts to prevent the operation of the voice-operated carrier control if an attempt should be made to transmit while speech is being received. Moreover, the disabler acts to block the passage of received speech which might pass through the hybrid and enter the transmitter. The relay is a slow-release type and remains closed for approximately half a second after cessation of speech, to avoid operation between syllables or short pauses.

The transmitting amplifier is similar to the receiving amplifier and includes the following: a single-stage speech amplifier to raise and control the level of the speech appearing on the telephone line before it enters the transmitter, a voice-operated carrier-control device and a 1000-cycle calling oscillator with amplifier. The carrier control circuit is essentially the same as the disabler which has been described, except that the relay, which is governed by the speech-triggered gas tubes, is arranged to operate the carrier control and changeover relay in the transmitter. The relay actuated by speech is arranged for a releasing time of approximately one quarter second, so as to hold in between syllables and short pauses in the speech.

One of the low-pass filters is connected in the output from the transmitting amplifier, and the other in the input to the receiving amplifier. The purpose of the former is to avoid modulating the transmitter by components of speech above about 2,500 cycles. If these components lie in the vicinity of 3,000 to 5,000 cycles, they would tend to operate the relaying equipment which is responsive to these frequencies. The purpose of the latter is to exclude the signalling tones from the telephone line at the receiving end.

The tone generators are identical for the three frequencies in use, the only difference being in the constants of the oscillating circuit. The oscillator functions at all times, and the amplifier following it is blocked off at all times except when it is required. This blocking is removed when a tone initiating relay on the transmitter is closed by the operation of the 25-cycle protective system.

The filter amplifiers are identical for the three frequencies in use, the only difference being in the elements of the band-pass filters which are employed. Each consists of a two stage amplifier that is bridged on the output of the radio receiver. The first stage serves to isolate the filter from the line and to allow control of level. The second stage is a power pentode whose output is rectified, and the resultant direct current is used to actuate a contactor. Between the two stages, there is inserted a single section band-pass filter with a band width of approximately 400 cycles. The mid-band frequencies are 3,000, 4,000, and 5,000 cycles. The contactor is energized by the rectified output of the amplifier following the filter, and its contacts operate into the 25-cycle protective system.

The calling unit is a combination of subsidiary apparatus. It includes the hybrid coil and balancing network

which couple between the two-wire telephone line and the four-wire communication channel represented by the transmitter input and the receiver output. A small rectifier of the dry-disk type with a filtering system is the supply for the local hand-set. The unit also carries sundry relays associated with the calling system. The low-frequency ringing current on the telephone line is converted into 1,000-cycle current which modulates the carrier. At the receiving end, the 1,000-cycle current operates both the loudspeaker and the relay which impresses the low-frequency ringing current on the called telephone line. The loudspeaker is cut off normally, being in operation only when a call is coming in.

Line Tuning Unit

This unit is mounted out of doors adjacent to the coupling capacitors. It contains the necessary coupling devices to transfer energy from the carrier frequency transmission cable to the 25 cycle high-voltage lines. Also it contains the loading inductances to tune out the reactance of the coupling capacitors. Two separate coupling and loading systems are contained in the unit, so that energy can be fed into two lines in any proportion. As the carrier frequencies are relatively high, the various coils are wound of stranded high-frequency cable. Air core coupling transformers are employed.

Operation

A condensed schematic diagram of the complete system for one terminal is shown in Figure 6.

(*a*) Signalling Tone Transmission

When a fault occurs near one end of the line which is 300 miles long, an impedance relay at the terminal near the fault closes its contact instantaneously. This operation trips the breaker at the near end, and at the same time starts the transmission of carrier modulated at 3,000 cycles by energizing the appropriate auxiliary tone-initiating relay which takes complete control of the carrier equipment at that terminal. This relay actuates the antenna changeover relay, which, in turn, mutes the receiver and starts both the carrier and the 3,000-cycle relaying or signalling oscillator. At the same time, the power-raising relay is actuated to increase the power of the transmitter to its full value. The transmitter then emits carrier, modulated only by the 3,000-cycle tone, for as long as the auxiliary tone-initiating relay is kept energized.

If the transmitter be in use at the time when a tone must be transmitted from the same terminal, the antenna changeover relay will be in the "transmit" position already, because of the action of the voice-operated carrier-control device. In this case, the tone-initiating relay overrides the talker, and the power is raised. The complete relaying operation will be as fast, or a little faster, than usual, because the carrier had been turned on previously. Moreover, the speech line is short-circuited to de-energize the voice-operated carrier control, and to avoid using up any of the modulation capability of the transmitter by the speech which is of secondary importance at the moment. As soon as the tone-initiating relay is de-energized, the system is restored to normal, and the remainder of the talker's conversation will be transmitted as usual, the power dropping back to its original level.

If two or three relaying tones are required, they can be modulated simultaneously at the expense of a small reduction in the modulation depth of each. This reduction is not so great as to impair the operation of the relays at the distant end.

(*b*) Signalling Tone Reception

The tones, demodulated from the carrier, appear at the output of the carrier receiver and enter the received tone amplifiers and filters. The low-pass filter blocks them from the receiving speech amplifier and prevents them from being heard by the listener if a conversation is in progress. The band-pass filters in the amplifiers separate the wanted tone from the others, or from speech, and the proper relay is actuated by the received tone. The contacts of this relay put a by-pass across the contacts of the timer on the second impedance range, making this range almost instantaneous.

If the equipment happens to be in use for transmission of one part of a telephone conversation, and a relaying tone is to be received, dependence is placed on the telephone cut-off relay, which is actuated by the second range impedance relays at the same time as the *JD* timer is started. This operation occurs at the same time as the relaying tone is started at the distant end. The telephone cut-off relay opens the circuit to the antenna changeover relay even though it has been closed by the talker. This relay is forced to go to the "receive" position, and the receiver is unblocked. The equipment is thus prepared to receive the tone at the same time as the carrier modulated by the tone arrives.

If the second range impedance relays energize the telephone cut-off relay at the same time as the tone-initiating relay is energized by the trip relay, then the tone-initiating relay overrides all else and the sequence proceeds correctly.

The time which is lost between energizing the tone-initiating relay at the end of the line nearer to the fault and the closure of the relay by the received tone at the distant end is in the order of 0.9 of a cycle on 25 cycles per second, if the equipment at both ends is in the stand-by condition. If the carrier equipment is in use for telephony, and the transmission is in the same direction as the relaying tone must be sent, the lost time is somewhat less, being about 0.5 of a cycle on 25 cycles per second. If the equipment is in use for telephony and the transmission is in the opposite direction to that which is required for the relaying tone, the total time lost is in the order of 1.5 cycles on 25 cycles per second. This is the slowest case because the system must be prepared to receive the tone as described previously.

A general view of the radio-frequency equipment is shown in Figure 7. At this station, these cabinets are 2,400 feet from the antenna tuning unit and about 2,000 feet from the panels on which are mounted the protective relays. The antenna tuning unit and a coupling capacitor are shown in Figure 8.

Conclusions

This carrier relay scheme which uses a tone as a transfer trip impulse, as described above, has several advantages. Standard protection relays of any type may be used. Only one carrier set is required at each station, coupled to as many lines as need this protection. The number of relaying tones or channels is limited only by selectivity of the band-pass filters. The carrier system operates as a telephone circuit normally, and only as a carrier relaying feature for an interval of one second for every line fault. A failure of the carrier increases the clearing time only for those faults which are in the end sections.

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Temperature Limits and Measurements for Rating of D-C Machines

F. A. COMPTON, JR.
ASSOCIATE AIEE

Synopsis: Existing industrial practice for determining the temperature rise of d-c machines is based on measurements with thermometers, placed on exposed or accessible surfaces of the machine. Tests using resistance measurements with improvements in technique have disclosed the existence of considerably higher internal temperatures than those indicated by thermometers, especially for short-time rated machines. Experience has indicated, nevertheless, that such machines have entirely satisfactory service records.

Greater accuracy and consistency are possible with the resistance method, with proper measurement techniques, and it affords a truer indication of winding temperatures. This paper, therefore, suggests that the resistance method should be more generally used and should ultimately be recognized in the Standards as is now the case for railway motors. With this in view, detailed recommendations for resistance-measurement technique are presented.

Correlation of the temperature rises determined by the resistance method with those measured by the thermometer method is necessary if resistance methods are to supplant the now standard thermometer methods. This paper shows the relation existing between temperature rises measured by thermometer and those measured by resistance on variously ventilated and insulated machines.

Values for temperature limits measured by resistance for continuous and short-time rated class-*A* and class-*B* insulated d-c machines are presented. These are consistent with the recently published AIEE Standard 1*A* "Report on General Principles for Rating of Electrical Apparatus for Short-time, Intermittent or Varying Duty." The information presented in this paper is intended to apply primarily to low-voltage (approximately 600 volts maximum) d-c machines with form-wound armature coils and conventional types of field coils.

Temperature Measurements

STANDARDS for rating d-c machines provide temperature limitations for the protection of winding insulation. Although there are three recognized methods of temperature measurement, namely,

1. Thermometer method
2. Resistance method
3. Embedded-detector method

the present AIEE practice is based on the thermometer method.

The embedded-detector method is used to determine interior temperatures at designated locations within windings. It is also used in small motor windings, with thermocouples, in specified locations inaccessible to mercury or alcohol thermometers.

The thermometer method is perhaps the easiest to apply, at least for open-type machines, and requires the least amount of equipment. Although the thermometer method has been used for many years with varying degrees of success, it indicates a temperature that is generally less than the internal temperature of the winding being measured. The thermometer may register a temperature very much less than the average copper temperature, depending on the size of motor, length of heat run, ventilation, internal temperature, and insulation. If within a motor one winding is 20 degrees centigrade hotter than the other winding, the thermometer on the cooler winding may actually register a temperature higher than the average copper temperature of that winding. In practically all cases, however, and especially for short-time ratings, the thermometer on the hottest winding will register a temperature less than the average copper temperature of that winding.

The thermometer method has met the needs of industry for many years, and is still adequate for practical purposes. However, it is difficult to obtain consistent results by the thermometer method, because of variations in surface conditions, thermometer locations, and ventilation. Quite often heat runs on duplicate machines or on the same machine

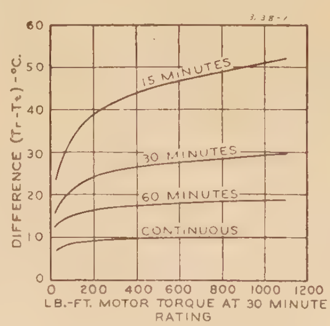

Figure 1. Temperature difference of resistance rise minus thermometer rise versus size of motor for various lengths of heat runs of class-*A*-insulated totally enclosed motors

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

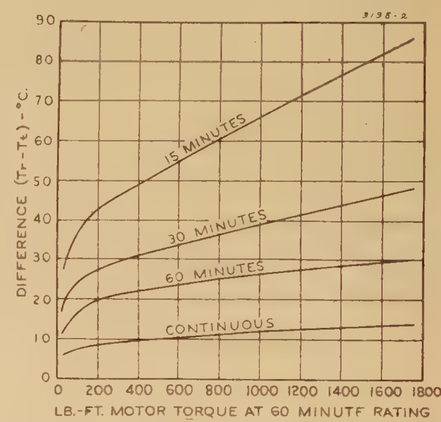

Figure 2. Temperature difference of resistance rise minus thermometer rise versus size of motor for various lengths of heat runs of class-*B*-insulated totally enclosed motors

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

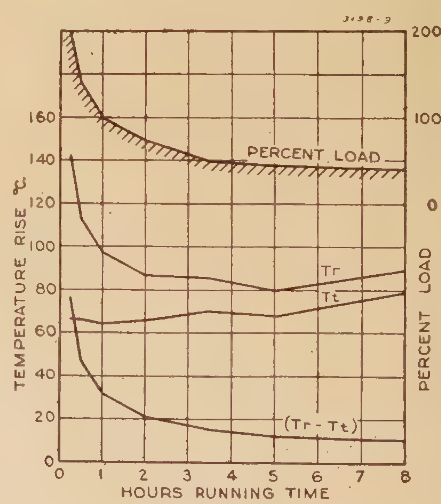

Figure 3. Temperature rise versus length of heat run on a totally enclosed motor

Shaded curve shows per cent load applied for each heat run

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

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F. A. Compton, Jr., is in the d-c armored motor engineering department, General Electric Company, Erie, Pa.

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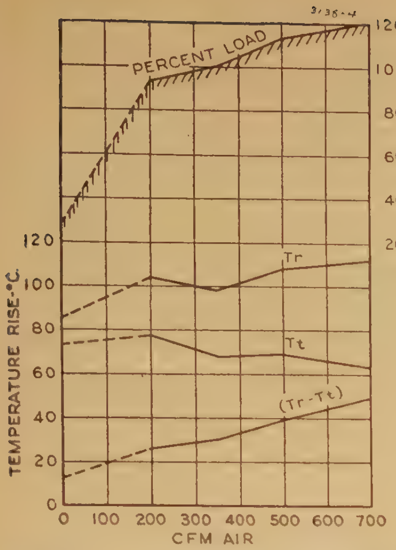

Figure 4. Temperature rise versus volume of ventilating air for continuous heat runs on a blown motor

Shaded curve shows per cent load applied for each heat run. Zero air heat run taken on totally enclosed motor

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

will not check previous thermometer tests with a reasonable degree of accuracy. Another shortcoming of the thermometer method is that it does not indicate the internal temperature of the winding.

The resistance method gives the average temperature of the copper in the winding being measured. This method requires more equipment than the thermometer method, but, when it is applied with the proper technique, more accurate and consistent results may be expected. The main advantage of the resistance method is that it indicates the average copper temperature within the winding, which is generally closer to the hot-spot temperature in any part of the winding than would be indicated by the thermometer method.

The rise by resistance is, in general, higher than the rise by thermometer, and the former should, therefore, be a better indication of the temperature which limits insulation life.

Temperature Ratings

For many years it has been the accepted practice to limit the temperature rise on d-c machines to predetermined values, depending on the class of insulation, ambient temperature, and degree of enclosure. The allowable temperature rises were derived from hot-spot temperatures, which were established as being the maximum that a particular class of insulation could withstand continuously and give satisfactory winding life. AIEE Standards 5 (D-C Rotating Machines, Generators and Motors) and 45 (Electrical Installations on Shipboard) and American Standard *C*50 (Rotating Electrical Machinery) currently specify that the temperature rise be measured by thermometer, and that the limiting observable rises on insulated windings for a 40-degree-centigrade ambient temperature be as follows:

	Degrees Centigrade	
	Class-A Insulation	Class-B Insulation
Totally enclosed	55	75
Open and semienclosed	50	70

General-purpose machines with a service factor of 1.15 are rated 40 degrees centigrade rise for class-*A* insulation. Although the foregoing temperature-rise limits were established primarily for continuous-rated machines, they have been applied without change or allowance to short-time-rated motors. The most commonly used short-time ratings are 30 minutes and 60 minutes, but 15-minute ratings are also used. The majority of crane motors carry a 30-minute rating, although they may be used in continuously repeated duty-cycle service. Marine-type motors for deck machinery are rated on a short-time basis of 30 or 60 minutes, generally on the former of these two bases. Since the majority of crane motors, and practically all marine deck motors, are totally enclosed, they have been built with short-time horsepower ratings limited by temperature rises of 55 degrees centigrade for class-*A* insulation and 75 degrees centigrade for class-*B* insulation, as determined by the thermometer method of measuring temperature. Many thousands of such motors have been in service for years and have given satisfactory performance. The satisfactory life of the insulation in such short-time-rated motors is due as much to the careful applications of the motors as to the temperature rise on which their rating is based. These motors, although rated on a short-time basis, are generally applied on a duty-cycle basis. In applying such motors, it is common practice to calculate the root-mean-square load, or the average watts loss of the cycle, to make sure that the motor has sufficient thermal capacity, assuming that the duty cycle is to be repeated continuously or for a specified period, so that it can be operated without the allowable temperature rise being exceeded.

Thus it is seen that a short-time horsepower rating is merely a "gauge" which gives the user a conception of the physical size and torque capacity of the motor, but which conveys nothing concerning the continuous thermal capacity of the motor. Although the temperature rise measured by thermometer at the end of the short-time heat run is equal to or less than the allowable rise of 55 degrees centigrade for class-*A* or 75 degrees centigrade for class-*B* insulation, the rise indicated by resistance measurements is greater than these values.

In a previous paper[8] data were presented to show the difference between temperature rises of d-c machines when determined by the thermometer method as compared with those determined by the resistance method. These data were taken from tests on class-*B*-insulated totally enclosed motors. The present paper presents additional data on differences between rise by thermometer and rise by resistance. Information is given on totally enclosed, self-ventilated, and blown motors, with class-*A* and class-*B* insulation, and on various sizes of motors.

The amount by which the rise measured by resistance exceeds the rise measured by thermometer for various time ratings for various sizes of totally enclosed motors is shown in Figures 1 and 2. The temperature-rise differential (resistance rise–thermometer rise) is shown plotted as a

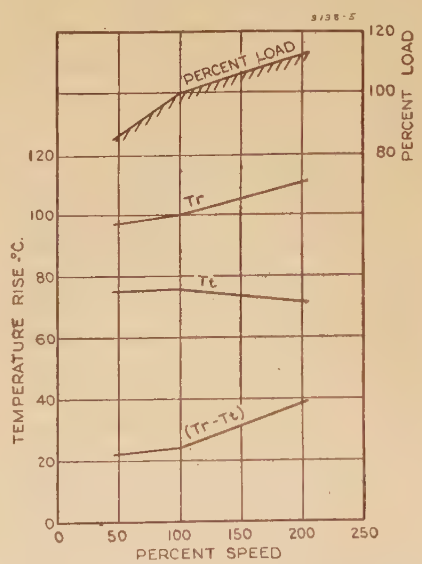

Figure 5. Temperature rise versus speed for continuous heat runs on a self-ventilated motor

Shaded curve shows per cent load applied for each heat run

T_r = Temperature rise by resistance, degrees centigrade

T_t = Temperature rise by thermometer, degrees centigrade

function of the 30-minute rated torque for a line of class-A motors and as a function of the 60-minute rated torque for a line of class-B insulated motors. These curves are plotted in this manner to show the general relation that exists between the various time ratings for a given size of motor, as well as the relation for a given time rating for various sizes of motors, the torque rating being an indication of the physical size of the motor. All heat runs from which the curves of Figure 1 were derived were made to give approximately 55 degrees centigrade rise by thermometer on the hottest winding. Likewise the heat runs for Figure 2 were made to give approximately 75 degrees centigrade rise by thermometer. In practically all cases, the armature was the hottest winding. Hence, the curves of Figures 1 and 2 are all based on armature temperatures. All thermometer measurements were made with mercury thermometers.

From these curves, general relations with respect to temperature differential (resistance rise–thermometer rise) can be expressed as follows:

1. For a given size of motor and thermometer rise, the shorter the time rating the greater the temperature differential. The reason for this is that, the ratio "watt-hours absorbed to watt-hours dissipated" is greater, the shorter the time. The watt-hours absorbed by the windings raise the copper temperature (resistance rise) while those dissipated raise the surface temperature (thermometer rise).

2. For a given time rating and thermometer rise, the larger the machine, the greater the temperature differential. The reason for this is that, the larger the machine, the greater the ratio of watt-hours absorbed to watt-hours dissipated.

3. For a given size of machine and time rating, the higher the thermometer rise, the greater the temperature differential. The reason for this is that, for a given insulation, more watts per square inch are required to produce a higher thermometer rise on the surface of the insulation, and the greater the watts per square inch, the greater the temperature differential.

Figure 3 shows temperature rises both by the thermometer and by resistance obtained from a series of heat runs made on one totally enclosed motor. The shaded curve shows the value of load that was applied for each heat run, expressed as a percentage of the one-hour load.

Figure 4 shows the effect of ventilating air on the differential between resistance rise and thermometer rise for a blown continuous-rated constant-speed motor. Figure 5 shows a similar effect on a self-ventilated motor on which heat runs were taken at various speeds obtained by holding constant field strength and varying the armature voltage. From Figures 4 and 5, it can be seen that, for a given size of motor, the more the ventilating air, the greater will be the difference between the rise by resistance and the rise by thermometer for continuous heat runs with approximately the same thermometer rise.

Figures 1 to 5, inclusive, are based on an average of many tests and are intended to show some of the various factors that cause a variation in the difference between resistance and thermometer rises.

From the foregoing it can be seen that, although it has been standard accepted practice to rate machines 55 degrees centigrade (class A) or 75 degrees centigrade (class B) rise by thermometer, the rise by resistance exceeds that by thermometer even on totally enclosed continuous heat runs. AIEE Standard 1 now recognizes a temperature differential of ten degrees centigrade between resistance and thermometer measurements for continuous apparatus. This is very close to that obtained from tests on totally enclosed motors, as shown in Figures 1, 2, and 3. However, especially for short-time rated motors, the resistance rise often will exceed the thermometer rise by an amount considerably greater than ten degrees centigrade.

The question that immediately arises is how to reconcile the higher internal temperatures disclosed by this investigation and the satisfactory service experience recorded with the generally recognized temperature-rise limits of 60 degrees centigrade by resistance for class A and 80 degrees centigrade for class B, given in AIEE Standard 1 and various American Standard and International Electrotechnical Commission Standards. However, the temperature-rise value desirable for any given case depends on many factors besides the insulating materials themselves. The ultimate choice is largely a matter of economics, depending on the relative importance of weight, size, reliability, cost, and life expectancy.

For example, it is well known that 120 degrees centigrade rise is allowed for class-B insulated railway motors (with 25 degrees centigrade ambient temperature assumed), whereas only 60 degrees centigrade rise by resistance is normally allowed for the high-voltage armature windings of large alternators with class-B insulation (on the basis of 40 degrees centigrade ambient temperature). In the particular case of low-voltage d-c machines, with form-wound armature coils and the usual type of field structure that are alone considered in this paper, the following factors are favorable to the use of higher internal temperatures:

1. The low voltages (approximately 600 volts maximum), the mechanical requirements, and the form-wound construction necessarily result in extremely low dielectric stresses.

2. The low voltage and corresponding small insulation thickness considerably reduce difficulty from varnish-solvent expulsion and coil swelling that limit high-voltage coil temperatures.

3. The relatively short armature-core lengths customary for d-c machines minimize the difficulties from thermal expansion, which are serious in large a-c machines.

4. The type of construction of the d-c rotating armature, including the commutator and the end-binding bands, restricts the exposure of the windings, resulting in a relatively greater difference between internal and external winding temperatures than in the case for induction and synchronous machines. At the same time, the parts of the insulation subjected to the higher temperatures are less exposed to oxidation, moisture, and atmospheric impurities.

Conclusions

In view of the desirability of internal rather than external temperatures as a basis of rating, and the greater accuracy and consistency that may be obtained, it is recommended that improved methods of temperature measurement by resistance be included in the AIEE test code for d-c machines and considered for later adoption as a basis of rating in the Standards. For the latter purpose, the following values of limiting observable temperature rises for both fields and armature windings of d-c machines determined by the resistance method are suggested for consideration:

Clas of Insulation	Limits of Observable Temperature Rises Determined by the Resistance Method for 40 Degrees Centigrade Ambient (Degrees Centigrade)			
	Continuous	One Hour	30 Minutes	15 Minutes
A	65	75	85	95
B	100	110	125	140

1. The values agree with AIEE Standard 1A, Table II, short-time ratings, except for the 15-minute rating.

2. The values for class-B insulation agree with AIEE Standard 1 Table V, preferred values of standard observable temperature rise for short-time or other special ratings.

3. The values are somewhat more conservative than railway motor values AIEE Standard 11, Table II.

The suggested values are intended to apply to low-voltage (approximately 600 volts maximum,) d-c machines with form-wound armatures and conventional

field coils. The values are not intended to apply to other classes of machines where the type of construction and nature of application differ.

Satisfactory experience has been obtained with the suggested values of rise by resistance in machines that meet present thermometer standards. For continuous-rated highly ventilated machines, these values will limit the rise by resistance to a value less than is permissible with the present thermometer standards.

Appendix I. Resistance Measurements

In order to promote the general acceptance of the resistance method of determining temperature rises, and thereby further improve the technique of this method, the following description of a method for determining temperature rise by resistance is offered.

The temperature by resistance method gives the average temperature of the winding being measured. This method consists of comparing the resistance at the end of a heat run (hot resistance) with the resistance at a known temperature (cold resistance). The temperature rise at the end of a heat run is determined by the following formula, which applies to copper windings only:

$$T - t_a = \frac{R_T - r_t}{r_t}(234.5 + t) + t - t_a \qquad (1)$$

in which

R_T = resistance in ohms of the winding at temperature T degrees centigrade (this will be the hot resistance measured at an unknown temperature T at the end of a heat run)

r_t = resistance in ohms of the winding at temperature t degrees centigrade (this will be the cold resistance measured at a known temperature t)

t_a = the mean ambient air temperature in degrees centigrade during the last quarter of the duration of the test

$T - t_a$ = the temperature rise in degrees centigrade of the winding above the ambient temperature

A convenient method for determining temperature rise by resistance is as follows:

1. Plot the relation of per cent change in resistance as abscissa against the temperature in degrees centigrade as ordinate. This straight-line relationship is shown in Figure 6 in which a reference temperature of 25 degrees centigrade has been chosen. It is recommended that the scale for the per cent change in resistance be chosen large enough so that it can be read accurately to the first decimal point.

2. Measure the cold resistance of each winding in the motor, and record the temperature of the windings as determined by placing thermometers thereon. Correct the cold resistance from the measured temperature (thermometers on windings) to 25 degrees centigrade. A convenient table for this correction is Table I.

3. Measure the hot resistance at the end of the heat run, and from the percentage increase in resistance $100(R - r_{25})/r_{25}$ read from Figure 6 the corresponding temperature. Subtract the ambient or cooling-air temperature t_a to obtain the temperature rise.

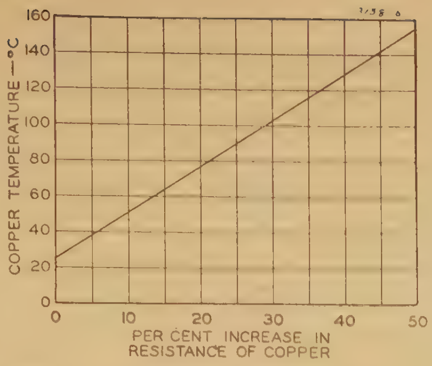

Figure 6. Temperature versus per cent increase in resistance of copper

4. To obtain the temperature rise at the instant of shutdown (at time the power was cut off), record the time at which each hot resistance was taken, the time being measured from the instant of shutdown. Take a series of hot-resistance readings at approximately two-minute intervals for approximately 20 minutes. These points can then be plotted as temperature rise (by resistance) as ordinates and time as abscissae. A curve drawn through these points and extrapolated back to zero time will give the temperature rise at the instant of shutdown. A typical cooling curve is shown in Figure 7.

In some cases, especially at the end of short-time heat runs, the slope of the cooling curve changes rapidly, and there is a large change in temperature during the first few minutes after power is off. It is therefore important to bring the machine to a standstill and take the first reading on each winding as quickly as possible. The cooling curve for the hottest winding generally has the greatest slope, and, since it is the limiting winding in determining the rating of the machine, care should be exercised in determining accurately the zero-time temperature rise on this winding.

There are two methods in common use for determining resistance

(a). The drop of potential, or voltmeter–ammeter method. This method is used to best advantage on resistance greater than five ohms.

(b). The comparison method in which the unknown resistance is compared with a known resistance by some suitable bridge. For measuring resistances less than five ohms, it is recommended that a Kelvin double bridge be used.

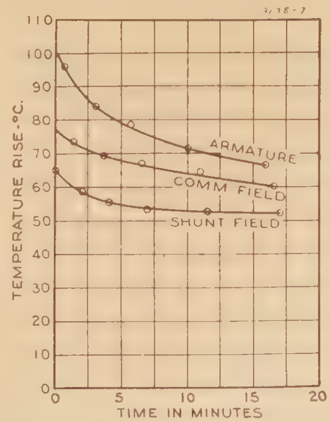

Figure 7. Typical cooling curves determined by the resistance method

Time is measured from instant of power off; taken after a continuous blown heat run

The success of measuring resistance by either the voltmeter–ammeter method or the bridge method depends to a great extent on the wiring and connections between the instruments or bridge and the machine windings being measured. Since for most d-c motors there are at least three elements to be measured—armature, commutating field, and main field—it is important that the wiring of the test table be so arranged that readings on all windings may be made at the end of a heat run in the minimum amount of time and by using only one set of instruments and/or one bridge. To accomplish this in the minimum time and with the least confusion, it is recommended that switches be used in the voltage circuits.

A convenient arrangement of test-table wiring for measuring cold and hot resistances is shown in Figure 8. Such an arrangement can be installed on individual test tables or on a portable resistance table which can be moved to any test stand.

Figure 8 shows the test-table wiring, the motor windings, and a Kelvin double bridge. The bridge is connected to the wiring by an ammeter jack and a voltmeter jack and can, therefore, be replaced readily by an ammeter and a voltmeter. In this diagram are included two extra sets of voltage leads, in addition to the three connected to the field windings which may be used for additional field windings, such as in a motor-generator set. The forks are used for measuring armature resistance. When field resistances are being measured, current is circulated through all field windings. By closing the proper switches, the voltage leads for each winding can be connected to the bridge individually.

Table I

Degrees Centigrade Temperature of Cold Resistance	K	Degrees Centigrade Temperature of Cold Resistance	K
15	1.040	26	0.996
16	1.036	27	0.992
17	1.032	28	0.989
18	1.028	29	0.985
19	1.024	30	0.981
20	1.020	31	0.977
21	1.016	32	0.974
22	1.012	33	0.970
23	1.008	34	0.967
24	1.004	35	0.963
25	1.000		

Multiply cold resistance by K to obtain resistance at 25 degrees centigrade.

The following precautions should be observed in wiring a table, as shown in Figure 8, and in taking resistance measurements to determine temperature rise. Although some of the precautions are not necessary for the voltmeter–ammeter method, they are necessary for the double-bridge method.

1. Each individual voltage lead between the bridge and the field windings should have approximately the same resistance as the other lead in the same pair and should be of relatively low resistance, preferably less than 0.1 ohm per lead.

2. All connections in the voltage leads must be clean and tight. All permanent connections should be soldered.

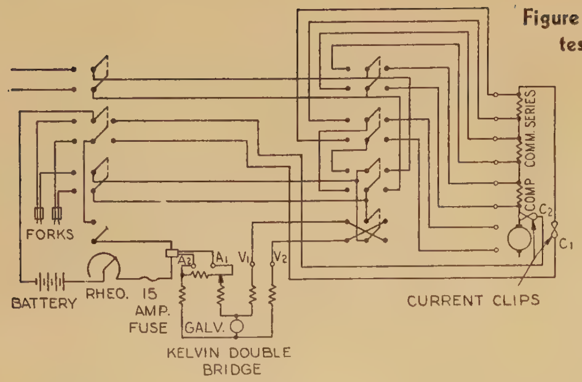

Figure 8. Schematic wiring diagram of test table for measuring resistance

3. All switches must be kept clean and free of dust or corrosion. The swivel connection on all switches between the handle and the base should be shunted with flexible copper shunts, one end of which is soldered to the switch blade and the other end to the connecting wire.

4. Pigtails should be soldered to the field-coil leads, so that the voltage leads can be bolted tightly to the pigtail and not connected at the joint where one field-coil cable connects to another cable.

5. For measuring armature resistance, the forks should be used. In each fork are a current and voltage lead soldered to brass pins (1/8 inch diameter), insulated from each other and held rigidly in the handle so that pressure can be applied without moving the pins. The forks should be applied to the ends of two commutator segments, securing the maximum span of segments that can be obtained between two adjacent brushholders. The armature should be so positioned that the brushes will not short-circuit armature coils between the span of the forks. The forks should be pressed firmly against the segments so as to break through any oxide film and make good contact with the copper.

6. Cold and hot resistances should be measured under as identical conditions as possible. For the armature, the bars used for determining cold resistance should be marked, and the same pair should be used in measuring hot resistance. In measuring the resistance of field coils, the voltage leads should be connected in such a manner that it will not be necessary to use the reversing switch after cold resistance is taken. The voltage leads should not be removed or changed between the times of taking cold and hot resistance. The current clips should be placed in the same relative position for both cold and hot resistance—this is very important, as will be explained later.

7. In order that errors in reading the double bridge may be reduced to a minimum, the lowest multiplying factor (see Appendix II) with which the bridge can be balanced should be employed. The measurement is independent of the current, but, the higher the value of current within the current capacity of the bridge, the greater will be the sensitivity of the bridge and, therefore, the accuracy of the readings.

8. Cold resistance should not be taken until all windings are within three degrees centigrade of room temperature.

9. As soon as power is cut off at the end of the heat run, the machine should be stopped as quickly as possible and all voltmeters disconnected, making certain that there are no paths in parallel with the windings to be measured.

10. The resistance of the current path between A_1 and the point where the current clip C_1 (Figure 8) connects to the series cable should be relatively low and of the same value for cold and hot resistance. The reason for this is explained as follows:

Refer to Figure 9 which shows the schematic connections of the Kelvin double bridge connected to an unknown resistance r_x.

The connection between A_1 and the point where V_1 lead is connected to r_x will be referred to as the d link. In Figure 9 the resistance of d may be zero or infinity, and as long as

$$(r_1+r_{v2})/r_2=(a+r_{v1})/b$$

then

$$r_x/r=(r_1+r_{v2})/r_2$$

where a, b, r_1, and r_2 are the resistances of the ratio arms of the bridge and r_{v1} and r_{v2} are the voltage lead resistances. For a ratio of 0.1, assume $r_1=100$, $r_2=1{,}000$, $a=100$, and $b=1{,}000$. When r_{v1} and r_{v2} are zero, then r_x (unknown) $=0.1r$ (standard). Assume $r_x=0.001$, and each voltage lead r_{v1} and r_{v2} to have one ohm resistance, then an error of one per cent will be introduced, because the standard resistance will have to be adjusted to 0.001 (1000/101) = 0.0099 in order to balance the bridge—that is, zero amperes through the galvanometer. Even in this case, as long as the ratio $(r_1+r_{v2})/r_2=(a+r_{v1})/b$ exactly, then the resistance of the d link, theoretically, has no effect.

Practically, it is not possible to maintain an exact ratio of $(r_1+r_{v2})/r_2=(a+r_{v1})/b$. Therefore, any resistance in the d link will introduce an error. The magnitude of this error will depend on the difference in ratios of the bridge arms, including leads, and the value of the d resistance.

When the bridge is balanced:

$$\frac{r_x}{r}=\frac{r_1+r_{v2}}{r_2}+\frac{d}{r}\left(\frac{b}{a+r_{v1}+b+d}\right)\times\left(\frac{r_1+r_{v2}}{r_2}-\frac{a+r_{v1}}{b}\right) \quad (2)$$

In comparing hot to cold resistance to determine temperature rise, approximately the same per cent error would be obtained in the hot resistance as in the cold resistance, thereby minimizing the error in temperature rise, provided that the resistances of the ratio arms, leads, and connections do not change between times of taking hot and cold resistance, and the d link resistance changes in proportion to the standard resistance r.

Since small differences in resistance occur in opening and closing switches, in contact joints, and with temperature change of wires, and the d link resistance does not change in proportion to the standard resistance, good practice dictates that the d link should have relatively low resistance, and the two voltage leads should have approximately the same resistance.

From Figure 8 it can be seen that when the resistance of the compensating field is measured, the commutating and series fields and connecting wires are in the d link. This same arrangement should be kept for both cold and hot resistance. Therefore, the current clips must be placed in the same relative position for both cold and hot resistance. For all practical purposes, the addition of this amount of resistance as compared to the winding being measured should introduce little or no measurable error in determining temperature rise, provided that the same relative connections are maintained for both hot- and cold-resistance measurements. The table wiring should be arranged so that it is not possible to connect the battery and rheostat into the d link.

The possible magnitude of error in resistance measurement caused by resistances in the d link and in the voltage leads is discussed in Appendix II.

The discussion in Appendix II is offered to point out the possibility of error in resistance measurement, and each person employing the double bridge will have to determine from actual experience and checking what refinements are necessary to give reasonably accurate results. If too many refinements are attempted, then the task of measuring resistance on heat runs becomes tedious, and the expense and trouble of such refinements may prove unwarranted. It must not be assumed mistakenly that a small amount of resistance in one of the voltage leads makes little or no difference, as is the case when using the voltmeter–ammeter method. Where a voltmeter may have a resistance of 1,000 ohms, 0.1 ohm inserted in one of the leads would give approximately 0.01 per cent error. The same value inserted in one of the voltage leads of the Kelvin double bridge may produce a considerable error, depending on the value of the unknown and the d link resistances.

The circuits shown in Figures 8 and 9 have been in use in one testing department for a number of years in determining temperature rise by resistance and have given reliable results. If such circuits are installed properly and operated with care, reliable results may be expected.

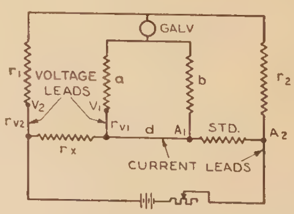

Figure 9. Schematic diagram of Kelvin double bridge

Appendix II. Kelvin Double Bridge

In the double bridge there are a number of adjustments provided in the ratio arms r_1, r_2, a, and b, in order to measure a large range of unknown resistances. For each adjustment $r_1=a$, $r_2=b$, and $r_1/r_2=a/b$. Although in portable forms of the Kelvin bridge $r_1=a$ and $r_4=b$ in most cases, it is not essential that this be so but only that the ratios be equal. The ratio of r_1/r_2 is the multiplying factor which, when applied to the value of observed resistance on the standard, r, gives the value of unknown resistance r_x.

For the purpose of discussion, it is assumed that the double bridge, Figure 9, has three multiplying factors to cover the range of 0.0001 ohm to 1.0 ohm resistance of r_x. It is also assumed that the various resistance arms within the bridge have the following values for each of the three multiplying factors:

Multiplying Factor	Resistance in Ohms		Range of Unknown (r_x) in Ohms
	r_1 and a	r_2 and b	
0.1	100	1,000	0.0001 to 0.01
1.0	500	500	0.001 to 0.1
10.0	1,000	100	0.01 to 1.0

The connecting wires between the bridge and the unknown resistance r_x (Figure 9) will introduce an error in the resistance reading. The magnitude of this error depends on the following factors:

(a). Resistance of the voltage leads r_{v1} and r_{v2}.

(b). Difference between the resistances of the two voltage leads $(r_{v1}-r_{v2})$.

(c). Resistance d introduced in the d link.

Two possible causes of error may be considered.

Case I where $d=0$. From equation 2 $r_x=r(r_1+r_{v2}/r_2)$. In this case an error will be introduced by the resistance of the voltage lead r_{v2} which will change the ratio of r_1/r_2 to $(r_1+r_{v2})/r_2$. Therefore, low-resistance voltage leads should be used, and it is preferable that each lead be less than 0.1 ohm. With a multiplying factor of 0.1 and $r_{v2}=0.1$ ohm, the ratio becomes 100.1/1,000 which gives an error of 0.1 per cent, a value that may be considered negligible. The resistance of the V_1 voltage lead r_{v1} will have no effect on the results so long as d is zero, a condition that may never be realized in actual practice.

Case II where $d>0$ and r_{v1} or $r_{v2} \gtreqless 0.1$ ohm. In this case a change in ratio of the first term on the right-hand side of equation 2 due to r_{v2} can be neglected, because the error will be 0.1 per cent or less, although the second term becomes significant. The error caused by the resistances of the voltage leads and the d link is directly proportional to the d-link resistance and to the difference $(r_{v1}-r_{v2})$ in resistance between the two voltage leads. Therefore, voltage leads of approximately equal resistance should be used as well as a low d-link resistance to minimize the error.

The following general relations apply when there is a difference in the voltage-lead resistance, $r_{v2}\neq r_{v1}$ and $d>0$.

1. For multiplying factors of 0.1 and 10:

Approximate per cent error
$$=0.091(r_{v1}-r_{v2})d/r_x$$

2. For a multiplying factor of 1.0:

Approximate per cent error
$$=0.1(r_{v1}-r_{v2})d/r_x$$

When $r_{v1}>r_{v2}$, the bridge will indicate a value greater than r_x.

When $r_{v1}<r_{v2}$ the bridge will indicate a value less than r_x.

From relations 1 and 2 it can be seen that, the lower the unknown resistance, the greater is the probability of error due to poor wiring and connections. These relations apply only to a limited range of resistance of $(r_{v1}-r_{v2})$ and d. They are, however, approximately correct for values greater than should be permitted in actual practice.

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A New Simple Calculator of Load Flow in A-C Networks

WALDO E. ENNS
ASSOCIATE AIEE

LOAD-FLOW analysis in a-c networks is increasing in importance because of the greater use of loop circuits in transmission and distribution systems to obtain the advantages of network operation.

To make the best use of materials and man power and to keep system investment at a minimum require careful system planning in advance of working up the details of design. If a simple device is made available to engineers responsible for this system-planning work which will enable them to make more complete analysis of system load flow, voltages, losses, reactive requirements, and so forth, in less time than heretofore possible by analytical solutions, it should aid materially in improving the over-all effectiveness and efficiency of the engineers and the systems they design.

A-c boards, network analyzers, or calculators have been developed to a high degree of perfection and are used extensively by the larger operating companies for major problems involving system interconnections and extensive changes or additions. These boards are useful in many ways, and the only objection to them is that they are relatively large and expensive, and for economic reasons they are not available to the majority of engineers except at times when the larger studies are involved. Every power system has many problems arising from day to day which could be answered better and more completely in less time if there was a simple calculator at hand.

It is the purpose of this paper to set forth certain broad principles which form the basis for the design of entirely new and simpler types of calculators for load-flow analysis of a-c networks. Calculators based on these principles may prove to be the answer to system-planning engineers' requirements for small inexpensive units which will be on hand to solve these problems as they arise. The description of one particular form of calculator is given to illustrate the application of these principles to a specific and practical design. Briefly, this calculator consists of a multitap voltage transformer, a few multitap resistors, a measuring instrument which is similar to a three-phase, three-wire electrodynamometer type of wattmeter, and a number of multicontact multiposition bus switches. In addition to these main elements, there are multiconductor cords with plugs and jacks as required for the interconnection between the units. No generator or load units are required, and the only circuit elements needed are enough multitap resistor assemblies to set up the maximum number of circuits connected to any one bus of the network. For example, if the actual network under study has 100 circuits, and the maximum number of circuits connected to any one bus is six, only six resistor assemblies are required to obtain a complete network setup so that readings can be taken at every bus for generation or load and for load flow in everyone of the 100 circuits.

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WALDO E. ENNS is electrical engineer with the Portland General Electric Company, Portland, Oreg.

In references 1 and 2 a new method of a-c network analysis using resistance networks is described. It was shown how the method could be applied to the conventional d-c board by making four separate resistance-network setups to simulate the a-c impedance network. It was further indicated that, by use of a new type of resistance board in which the four resistance networks can be set up simultaneously and handled the same as a single network, it is possible to read directly real and reactive power, current and voltages at all points in the network by means of special metering equipment. This type of board referred to in these previous papers may be classified as a constant-current type, because the generators and loads are setup in terms of current components.

As an outgrowth of the study to obtain direct readings of real and reactive power for the constant-current type of board, the principles fundamental to the simple calculator described in this paper were developed. It may be classified as a constant-potential type, because voltage components are used to set up the generation and loads on the system. The advantage of the constant-potential over the constant-current type is that it requires much less equipment.

On existing a-c boards the load unit settings for real and reactive power are obtained by first assuming what the voltage at each load bus will be under the desired loading condition on the network under study and then computing the resistance and reactance combination which will give the approximate load at each bus. After generation has been set up on the board in approximately correct amounts, it is possible to readjust the loads and generators until the board is brought into correct adjustment for recording the readings. The generators are provided with phase-angle and voltage adjustment, so that the desired amount of real and reactive power can be established by a series of adjustments between generators and between generators and loads. Therefore, before good speed can be attained on setting up an a-c board for load-flow studies, the operator must first learn by experience the technique of bringing the board into correct adjustment for a setup of a particular network and loading condition. After the board is adjusted correctly for one set of readings, it is usually a simple matter to readjust it for other loading conditions or network changes, if the changes in loadings and circuits are not too great.

On the new calculator, described in this paper, the constant-potential source may be either alternating or direct current as desired, but the a-c type is somewhat simpler, and therefore the description is limited to this type. Since the circuit elements are made up of resistors only, it is evident that the flow established through these elements is real power and is of no importance except that it does determine the watt rating of the resistors used in the design of the calculator. The manner in which resistance circuit elements are set up with voltage components impressed across them to obtain a simulated flow of real or reactive power through them is the basic principle of the new calculator. Although one means of measuring this simulated real and reactive power flow is described, there are other methods of accomplishing these measurements.

The simulated real and reactive power flow on the new calculator is obtained by setting up the inphase and quadrature components of voltage, which in the operator's judgment would be likely to exist at each bus in the network under study with a particular loading condition. In effect, this is similar to the technique used in setting up the generator units on an a-c board of the conventional design.

Because the loads, as well as the generators, are set up in this way, it is evident that more adjustments will be required to bring this new calculator into correct balance for a particular loading condition on the network. Therefore, as with existing a-c boards, the speed with which results can be obtained with this new device depends on the technique of the operator in making a series of adjustments at generator and load points to bring the board into correct balance for reading. It may be granted that the new board requires a greater number of adjustments to obtain a correct balance which will consume additional time. However, this does not mean that the device will not make an excellent showing as compared to other means of analyzing load flow in a-c networks. Also, as with existing a-c boards, after a network has been set up and brought into balance for one particular loading condition, if changes are made in the network or in the loading that are not too great, it will be a simple matter to readjust this board to suit the new conditions.

There are some points in the favor of the new calculator in addition to its extreme simplicity in design, small size, and low cost. One point is that it could be handled easily by a single operator. A second point is that, because of the small number of resistances required for circuit elements, they can be of a wattage rating that will allow substantial amounts of current to flow, so that the sensitivity of the measuring instrument need not be so great. A third point is that, because of its simplicity, the network connections can be made in a minimum time. A fourth point is that, by the elimination of generator and load units, it does not necessitate the reduction of an extensive network in advance to bring it down to a certain number of generators or loads before it can be set up on the board.

The circuit theory which is fundamental to this new calculator was presented in detail in the two references and therefore only a brief summary of this theory will be given as a background for the development of the equations for real and reactive power which are basic to the design of the new constant-potential type of calculator.

Mathematical Basis for New Calculator

If a vector current $\boldsymbol{I}=I_p+jI_q$ is flowing through a three-phase circuit of impedance $\boldsymbol{Z}=R+jX$ ohms to neutral, the voltage drop to neutral is $\boldsymbol{V}=V_p+jV_q=(I_pR-I_qX)+j(I_qR+I_pX)$ where vector $\boldsymbol{V}$ refers to the same reference axis as vector $\boldsymbol{I}$.

If a vector voltage drop $\boldsymbol{V}=V_p+jV_q$ occurs in a circuit with impedance $\boldsymbol{Z}=R+jX$, then the vector current flowing will be $\boldsymbol{I}=I_p+jI_q=(GV_p+BV_q)+j(GV_q-BV_p)$ where conductance $G=R/Z^2$ and susceptance $B=X/Z^2$.

If the voltage to neutral at one end of the circuit is $\boldsymbol{E}=E_p+jE_q$ volts, then, with balanced loading on the three phases, at this end the real power is $P=3(E_pI_p+E_qI_q)$, and the reactive power is $Q=3(E_pI_q-E_qI_p)$.

By substitution these equations P and Q may be rewritten

$$P=3E_p(GV_p+BV_q)+3E_q(GV_q-BV_p)$$

and

$$Q=3E_p(GV_q-BV_p)-3E_q(GV_p+BV_q)$$

To put the equations in a more useful form for the calculator design, let $M=1/G$ and $N=1/B$.

$$P=3E_p(V_p/M+V_q/N)+3E_q(V_q/M-V_p/N)$$

and

$$Q=3E_p(V_q/M-V_p/N)-3E_q(V_p/M+V_q/N)$$

In case it is desired to obtain the total net real and reactive power flow in a group of circuits connected to a common bus, the equations may be rewritten as follows:

$$P=3E_p[\Sigma(V_p/M)+\Sigma(V_q/N)]+3E_q[\Sigma(V_q/M-\Sigma(V_p/N)] \quad (1)$$

$$Q=3E_p[\Sigma(V_q/M)-\Sigma(V_p/N)]-3E_q[\Sigma(V_p/M)+\Sigma(V_q/N)] \quad (2)$$

Equation 1 states mathematically that the total net real power in a group of circuits connected to a common bus is equal to the algebraic sum of the real power flow in the individual circuits which combine to make up the group. Equation 2 is similar, except that it covers the same case for reactive power. These two equations are fundamental to the design of the new calculator, and this will be shown by illustrative diagrams later in the paper.

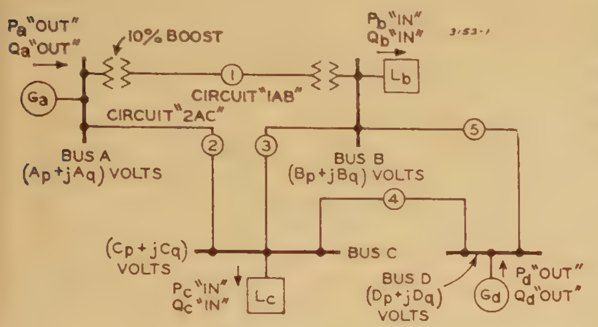


Figure 1 (left). One-line diagram of three-phase network to be used to illustrate connections and operation of calculator

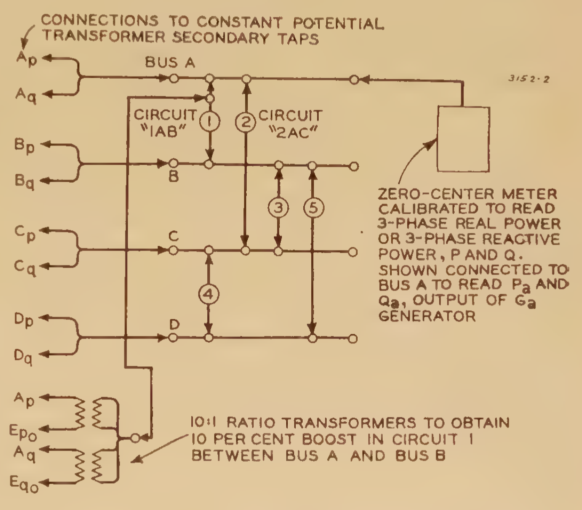


Figure 2 (right). Plugging diagram for network of Figure 1, as it would be connected on the calculator

If a generator supplies a group of circuits from a bus, the algebraic sum of the real power flowing in the circuits is a measure of the real power supplied from the generator. Likewise the algebraic sum of the reactive power in the circuits is a measure of the reactive power supplied from the generator.

If a load is supplied from a bus which in turn is supplied from a group of circuits, then the algebraic sum of the real power flowing in the circuits is a measure of the real power supplied to the load. Likewise, the algebraic sum of the reactive power in the circuits is a measure of the reactive power supplied to the load, or, in case the load has leading power factor, it will be a measure of the reactive power supplied from the load.

The convention used in this paper is that power flow in a circuit, or group of circuits, is "out" if it flows away from the bus. As indicated on a zero-center wattmeter, "out" is to the right of zero. If power flow is toward the bus, it is "in"

and would be indicated to the left of zero center on the instrument scale.

Reactive power flow in an indicated direction usually carries a negative sign to make it conform to the mathematical relationship it has with the quadrature component of current. In this case, however, the negative sign may be disregarded by using the convention of "out" and "in" which corresponds to the convention previously described for real power.

Calculator Operating Technique

The best way to establish an operating technique for this new calculator is by experience with the device, but it is possible to point out several factors which will aid in determining what procedure might be used in setting up the voltage components for a test on a network.

For the (X/R) ratios usually encountered in the circuits of an electric-power system, the following generalizations may be made:

1. At generator busses, both E_p and E_q would be set at a higher positive voltage tap than at load busses, because real and reactive power flowing in the same direction in a circuit will cause a drop in E_p and E_q. The amount of the drop in the two components will depend on the amount of real and reactive power flowing and the values of X and R for the circuit.

2. At load busses close to generation, both the E_p and E_q would be set at a higher positive voltage tap than at load busses more remote electrically from generation.

3. At load busses where reactive power is supplied to the network because of a synchronous condenser or other capacitance, then the E_p component may be more positive than that at surrounding busses, even though the real power flow is toward this bus. The value of E_q will be less positive or more negative than that of the surrounding busses in this case.

Description of New Calculator as Illustrated by Figures 1 to 4, Inclusive

Figure 1 shows the one-line diagram of a simple three-phase a-c network which is used as a basis for illustrating the setup and operation of the new calculator for determining load flow. Although the network is small and is self-explanatory on the diagram, it should be noted that it covers the pertinent points of generation, loads, and one circuit with step-up and step-down transformers set at different taps so that a net boost is effective in the closed loop of which this circuit is a part.

In setting up the network of Figure 1 on the calculator, it is necessary to establish the circuit constants $M=Z^2/R$ and $N=Z^2/X$ on the common kilovolt-ampere or voltage base of the board. In the case of circuit $1AB$ which includes the two transformers, the lumped-impedance method is used as the basis for obtaining the values for M and N for circuit $1AB$, and then a ten per cent series boost is inserted in this lumped circuit from A to B.

Figure 2 shows the schematic plugging diagram for the network of Figure 1 as it would be set up on the calculator. The movable cords, or other means of adjusting connections, are indicated by the heavy lines with the plugs and jacks shown as arrows and small circles, respectively. It should be noted that the plugging arrangement is simple and straightforward so that the connections can be made quickly.

Figure 3 shows the complete wiring for the fundamental circuit for measuring P, real power at bus A of the network. This diagram illustrates how the equation for power in terms of voltage components E_p, V_p, E_q, V_q, and circuit constants M and N may be set up with a measuring instrument to obtain a reading of real power flow. The instrument shown is essentially the same as a three-phase three-wire electrodynamometer type of wattmeter with a zero-center scale. The potential coils connected 1–2 and 7–8 are the moving coils whose combined torque act together to move the pointer. The current coils 3–4 and 5–6 are fixed coils and work together with the potential coil 1–2 to produce the torque of the upper element of the instrument. Likewise the current coils 9–10 and 11–12 are fixed coils which work together with potential coil 7–8 to produce the torque of the lower element of the instrument. The only unusual feature insofar as the instrument is concerned is that there are two fixed current coils on each of the two elements.

Referring to Figure 3, it will be seen that $(E_p)_a$, the inphase component of voltage to neutral at bus A, is acting across potential coil 1–2; that the current in coil 3–4 is the algebraic sum of $(V_p)_{ab}/M_{ab}$ and $(V_p)_{ac}/M_{ac}$; further that the current in coil 5–6 is $(V_q)_{ab}/N_{ab}+(V_q)_{ac}/N_{ac}$. The torque developed in the top element of the instrument is proportional to

$$(E_p)_a[(V_p)_{ab}/M_{ab}+(V_p)_{ac}/M_{ac}+(V_q)_{ab}/N_{ab}+(V_q)_{ac}/N_{ac}]$$

If the potentials and currents are traced for the lower element of the instrument, it will be seen that the torque developed is proportional to

$$(E_q)_a[(V_q)_{ab}/M_{ab}+(V_q)_{ac}/M_{ac}-(V_p)_{ab}/N_{ab}-(V_p)_{ac}/N_{ac}]$$

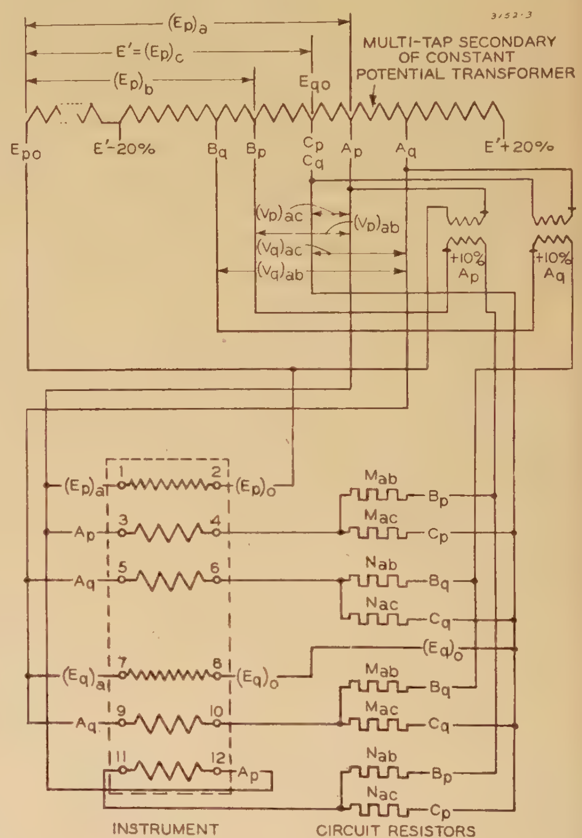
The sum of the upper- and lower-element torques is proportional to $P/3$ in the equation for power, P. The instrument

Figure 3. Schematic diagram of connections when measuring power flow at bus A of the network of Figure 1

Fundamental equation for three-phase power:
$P=3(E_pI_p+E_qI_q)$ where E_p and E_q are to neutral

Measurement of three-phase power fed to bus A by generator G_a is obtained by measuring the algebraic sum of the power flowing from bus A to bus B in circuit 1 and to bus C in circuit 2

$$P_a=3(E_p)_a\left[\frac{(V_p)_{ab}}{M_{ab}}+\frac{(V_p)_{ac}}{M_{ac}}+\frac{(V_q)_{ab}}{N_{ab}}+\frac{(V_q)_{ac}}{N_{ac}}\right]+3(E_q)_a\left[\frac{(V_q)_{ab}}{M_{ab}}+\frac{(V_q)_{ac}}{M_{ac}}-\frac{(V_p)_{ab}}{N_{ab}}-\frac{(V_p)_{ac}}{N_{ac}}\right]$$



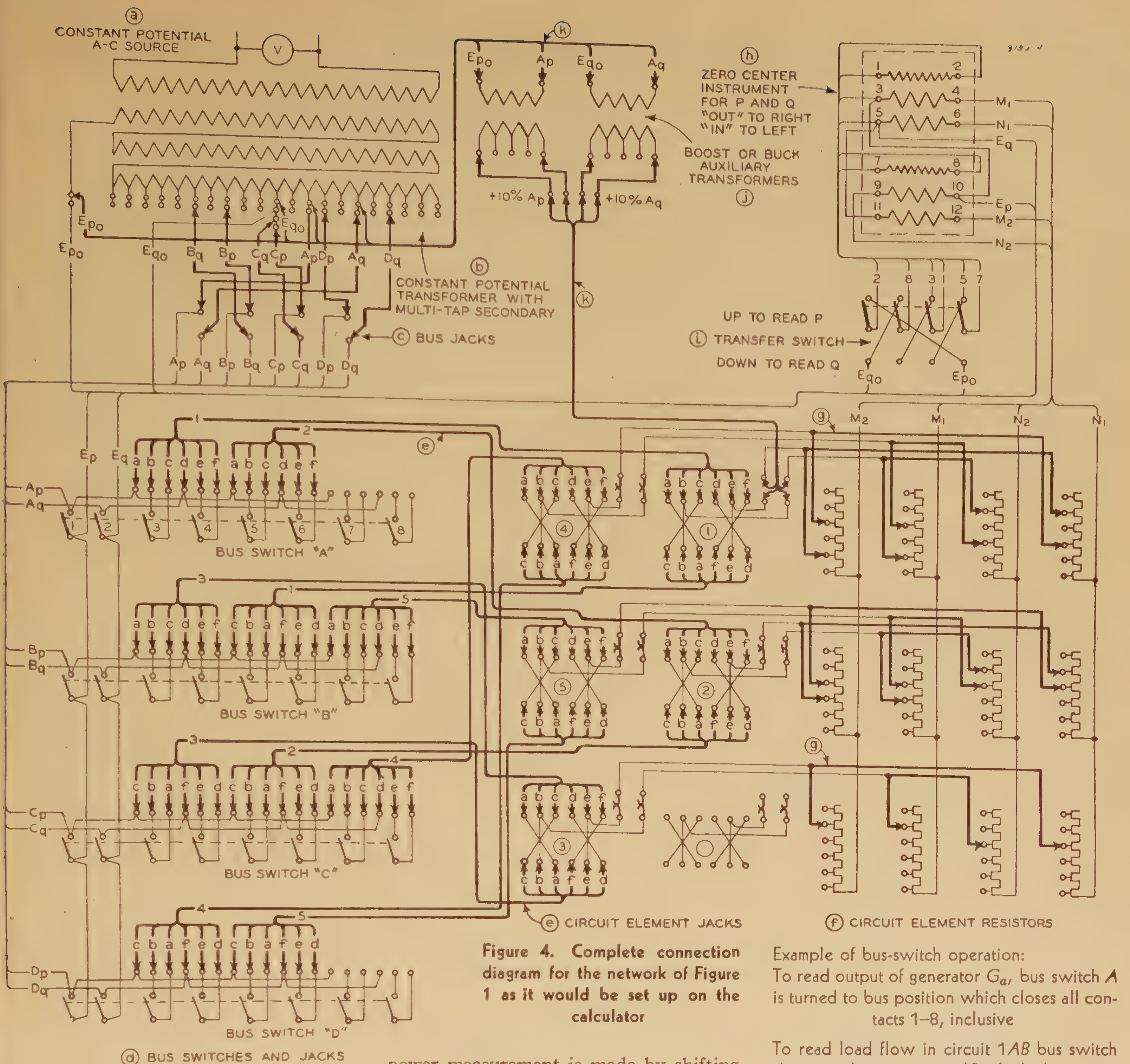


Figure 4. Complete connection diagram for the network of Figure 1 as it would be set up on the calculator

Example of bus-switch operation:
To read output of generator G_a, bus switch *A* is turned to bus position which closes all contacts 1–8, inclusive

To read load flow in circuit 1*AB* bus switch *A* is turned to position 1*AB* which closes contacts 1–4, inclusive, but opens all other contacts

To read 2*AC* bus switch *A* is turned to 2*AC* which closes contacts 1, 2, 5, 6 but opens all other contacts

Circled letters refer to items described in paper

— Flexible connection cord

O← Plug inserted in jack or one point of a polarized plug on a multiconductor cord

— Lightweight lines indicate fixed wiring

scale calibration may be made to read the three-phase power, *P*.

This reading for power is the sum of that flowing in the two circuits 1*AB* and 2*AC*, and therefore it is also equal to the generator G_a output.

To obtain the power flow in the circuit 1*AB* it is only necessary to turn the bus *A* switch to position 1*AB* which disconnects 1*AC* circuit from the meter. Likewise, to read power flow in 1*AC*, the bus *A* switch would be turned to 1*AC* position which disconnects 1*AB* circuit from the meter. This bus switch is not shown on Figure 3, but it is shown on the more complete diagram on Figure 4.

Because of the similarity of the reactive-power measurement to that described for real power, a separate diagram and discussion are not included. The reactive-power measurement is made by shifting E_p and E_q potential connections with respect to the currents by means of a transfer switch as shown on Figure 4.

Figure 4 is a complete wiring diagram of one arrangement for the new constant-potential resistance-circuit type of network calculator. It meets the requirements of the simple plugging arrangement of Figure 2 for the complete setup of the networks shown on Figure 1.

The calculator consists of the following elements:

(*a*). Constant-potential a-c source.

(*b*). Constant-potential transformer with multitap secondary winding.

(*c*). Plugging cords for connecting between transformers secondary tap jacks and bus jacks.

(*d*). Bus switches, multipole and multiposition.

(*e*). Plugging cords for connecting between bus-switch jacks and circuit-element jacks.

(*f*). Circuit-element resistors, multitap.

(*g*). Plugging cords between circuit-element jacks and circuit-element resistors, or the equivalent in multiposition switches.

(*h*). Zero-center measuring instrument for real and reactive power.

(*i*). Transfer switch to connect meter for real-power or reactive-power measurement.

(*j*). Boost or buck auxiliary transformers.

(*k*). Plugging cords for boost or buck transformers.

(*l*). Miscellaneous fixed wiring.

Figure 2 showed the fundamental plugging arrangement in a simple manner, and Figure 3 illustrated the basic principle of the calculator design, so that only a brief discussion is presented on some of the elements as shown on Figure 4.

(*a*). The constant-potential a-c source to the primary winding of the supply transformer should be regulated closely, and there are several static types of constant-voltage regulators on the market which would be ideal for this duty.

(*b*). The constant-potential a-c transformer with tapped secondary winding should be of ample design so that the regulation could be neglected and, by supplying a constant potential to the primary winding, the voltage at the secondary taps would remain at fixed values. The range for the taps as shown would allow for a 40 per cent difference in each of the voltage components over the network, from reference point E^1 or $(E_q)_o$ to plus 20 per cent of E^1, and from reference point to minus 20 per cent of E^1, where E^1 is the value of the reference voltage to neutral. From test computations the secondary taps may be at intervals of approximately 0.5 per cent of E^1. For simplicity on the diagram, taps are shown at intervals of two per cent of E^1.

(*c*). Plugging cords between transformer secondary taps and bus jacks could be replaced with multiposition switches if desired.

(*d*). The bus switches are shown with enough contacts to take care of three circuits per bus. These could be extended to take care of any number of circuits per bus. Probably a logical design would be to use about a six-position switch which would take care of metering the total at the bus and five individual outgoing circuits, and then, when more circuits than five are connected to a single bus, additional bus switches would be assigned to that particular bus to take care of the additional circuits. It is possible to replace the bus switches with bus jacks and cords, but the switches should give a means of speeding up the taking of readings. Another alternate to the bus switches would be the use of small relays actuated by push buttons.

(*e*). The plugging cords between bus-switch jacks and circuit-element jacks are six-conductor flexible cords, all similar. The change in the rotation of the lettering between the connections to the top and bottom jacks is taken care of by cross connection between the jacks, as shown on the diagrams, so that all of the cord plugs are polarized in the same way.

(*f*, *g*). The circuit-element resistors are multitapped, and four resistors make up a single circuit element. These resistors can be set up in pairs, because two are for the M setting, and two are for the N setting. Although cords are indicated as the means of choosing the proper resistor tap, the same results may be obtained by having a suitable number of rheostat-type multicontact switches. As discussed earlier in the paper, the number of resistor units required for the calculator is dependent on the maximum number of circuits connected to any one bus. This means that each unit may be set up to simulate any number of circuits, provided they do not connect to a common bus.

(*h*). The operation of the zero-center measuring instrument is discussed fully in connection with Figure 3. There is one feature which has not been included. It should be provided with several scales and a multicontact switch for switching from one scale to another to suit the range required for a particular measurement. Probably the multirange would be obtained by the use of multipliers in the two potential circuits.

(*i*). The transfer switch for the measuring instrument operates to transfer the potentials, so that in the up position the instrument reads real power and in the down position it reads reactive power. Because of the simplicity of the board, it might be advisable to have separate instruments for real and for reactive power, so that, when changes are made in potential connections to bring the board into balance, the effect of the changes could be observed on the two instruments at the same time and thereby the adjustment might be speeded up.

(*j*). The boost or buck auxiliary transformers are provided to take care of the effect of net boost or buck in a circuit which forms one branch of a closed loop in the network. The impedance of these should be low as compared to the resistance values used in the circuit elements, so that the transformer introduces a series boost or buck without changing the circuit constants appreciably.

(*k*) and (*l*) require no explanation other than the diagram.

Conclusions

A network calculator, of an entirely new form, and of unusually small proportions in physical size and in the simplicity of its electrical elements, has been described in this paper. It may be used for analyzing a-c networks for load-flow and voltage conditions and for the determination of system losses and reactive requirements. In the form described, it is not applicable to the study of system fault conditions.

Although not specifically covered in the discussion, it is apparent that long lines may be set up on this type of calculator by using constants based upon the nominal or equivalent π. In such a case, the effect of the leading reactive and leakage can be included by adjusting the measurement of generation or load at the two terminals of the transmission line.

In the general discussion section of the paper the technique of operating this new calculator was compared to that now used for complete a-c boards of conventional design. The new calculator is not intended to be the equivalent of the complete a-c board but instead is designed to fill the gap which now exists between the a-c analyzer or calculator and complex analytical methods.

The writer intends to build this simple calculator as soon as it is possible to do so, and, on the basis of operating experience, to determine the board technique which will give the quickest results. If it proves to be slower than the complete a-c board and yet much faster than other methods of analysis, it is believed it will prove to be a completely satisfactory tool.

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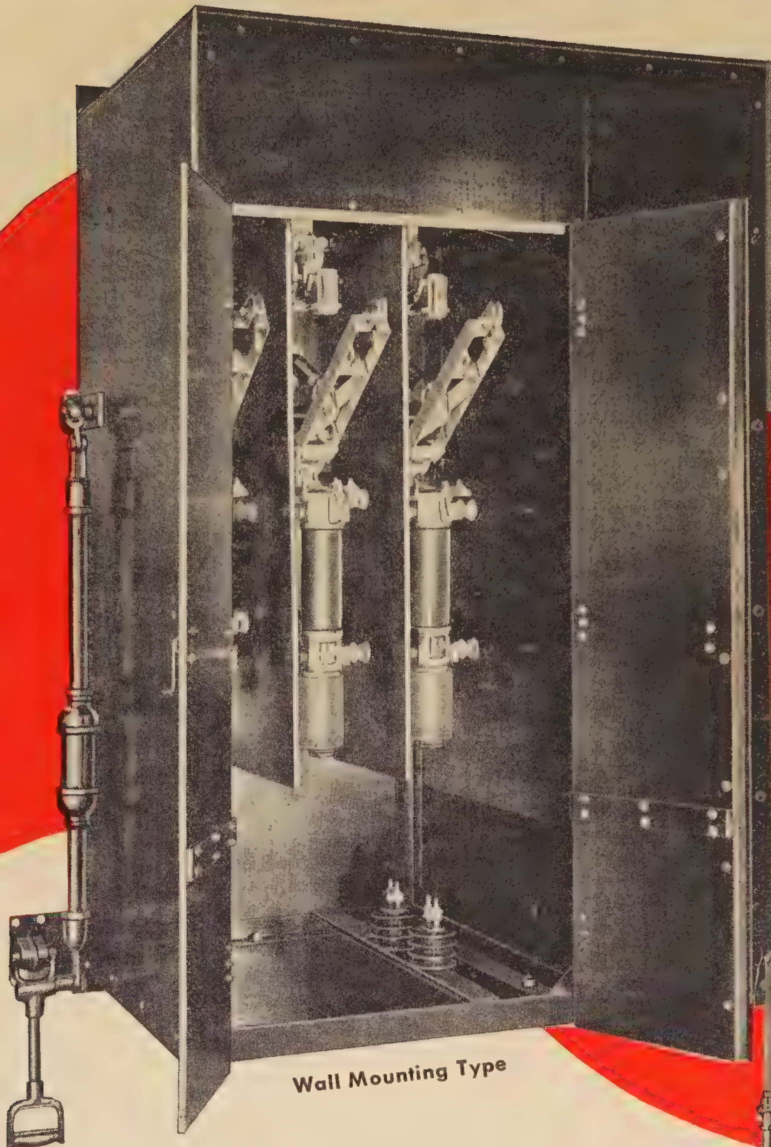

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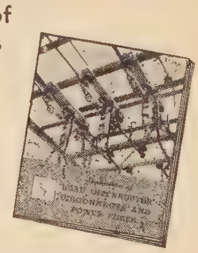
and Get Short-Circuit Protection with a Fuse Combination

● Manual switching of high voltage circuits—and reliable short-circuit protection are provided in one unit assembly,—in one cabinet, by the Load-Interrupter Disconnect and Fuse Equipment designed and pioneered by S & C.

The Load-Interrupter Switch is capable of interrupting up to 400 amperes at 15,000 volts without drawing an arc across the main switch contacts. The Heavy Duty Fuses have short-circuit interrupting ratings of:—20,000 r. m. s. amperes at 13,200 volts; 25,000 r. m. s. amperes at 6600 volts; 30,000 r. m. s. amperes at 2300/4000 volts.

For many services where all the functions of circuit-breakers are not essential, this load-switching and fuse combination assembly has found wide acceptance during the past few years, effecting savings in initial installation and maintenance costs, and conserving critical metals.

These assemblies are installed at substations, main and branch feeders, and employed as throw-over devices from two alternate feeders. Consult the nearest S & C office for further details of this equipment and its many applications,—or write today for Bulletins 202-A and 763.



SCHWEITZER & CONRAD, Inc.
4435 Ravenswood Avenue CHICAGO, ILLINOIS

MISSING

. . . yet still in action

For years, Manson Tape has occupied the highest position of all friction tapes. Whenever the utmost safety and permanence in splice protection was required, Manson Tape became the first choice of experienced engineers and electricians. Purchasing agents, too, willingly paid more to get lasting protection.

Contributing to this fine reputation of Manson Tape was the pledge of The Okonite Company to use only the best materials in its manufacture. One of these materials was a special type of natural rubber, which thoroughly impregnated and coated the strong, closely-woven cotton fabric. The exclusive use of this special type of new rubber was one reason why Manson Tape was unequaled in true adhesiveness and in aging and weathering qualities.

Today, with *all* new rubber forbidden for this use, the manufacture of Manson Tape has been temporarily discontinued because, if made with the materials currently available, Manson Tape would not have the superior characteristics nor be of the same high quality that you have always known.

As an assurance to the thousands of electrical men who have so consistently used it, we remind them that older joints and terminals made with Manson Tape will stay protected for years to come. To those who are fortunate enough to have available a supply of Manson Tape, we suggest it be reserved for the most important jobs.

UNTIL MANSON TAPE IS AGAIN AVAILABLE

We recommend as an alternative, *Dundee "A" Friction Tape* which fully complies with all current Federal and A.S.T.M. emergency specifications.

THE OKONITE COMPANY

PASSAIC NEW JERSEY

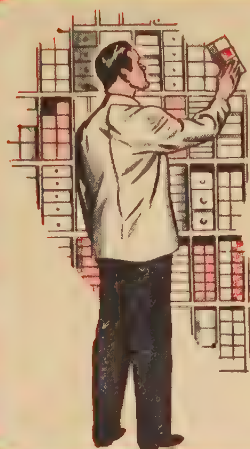
3436

MANUFACTURERS OF INSULATED WIRES, CABLES AND TAPES SINCE 1878

4 WARTIME SERVICES YOUR LOCAL RCA TUBE DISTRIBUTOR OFFERS YOU TODAY

1. Technical "Know how"

He started in Electronics on the ground floor . . . with *radio.* His advice and recommendations about circuits, tubes, and other electronic components are valuable to you.



2. Supply of Stocks

He's right around the corner; check with him first for items you need to fill on a rush priority basis.

3. Fast Delivery

If his normally adequate and varied stock should *not* include what you want, he's your one best bet today for locating it *fast!*

4. Intelligent Expediting

He's an old hand at tackling tough jobs. That's his business.
When you're in a jam, you'll find him well qualified to act as your expediting specialist.

Get to know this man. He's eager to help you. He's *your* EMERGENCY ELECTRONIC EXPEDITER! If you don't know where to find him, write us for the list showing your nearest RCA Tube & Equipment Distributor. RCA Victor Division, RADIO CORPORATION OF AMERICA, Camden, N. J.

TUNE IN "WHAT'S NEW?" RCA's great new show, Saturday nights, 7 to 8, E. W. T., Blue Network.

The Magic Brain of All Electronic Equipment Is a Tube and the Fountain-Head of Modern Tube Development Is RCA.

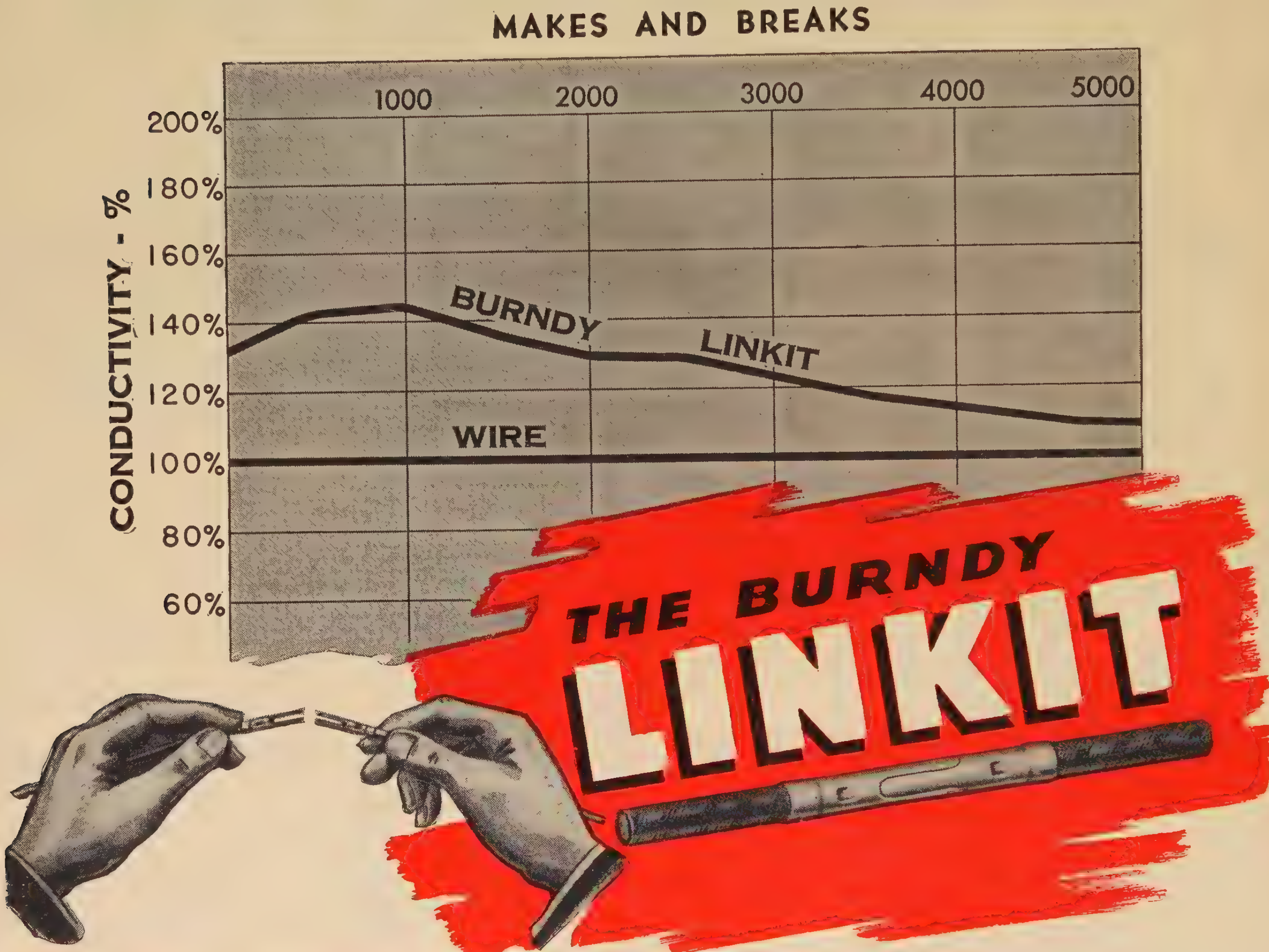
BUY WAR BONDS

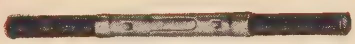
RCA ELECTRON TUBES

ASK ABOUT... RCA INDUSTRIAL MUSIC SERVICE

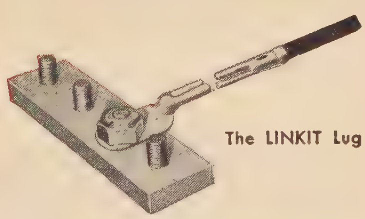
After 5000 makes and breaks

RELATIVE WIRE CONDUCTIVITY is still 110%!

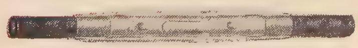




The Burndy LINKIT



The LINKIT Lug



LINKET with Burndy Versiflex insulating sleeve

Patent No. 2,109,837

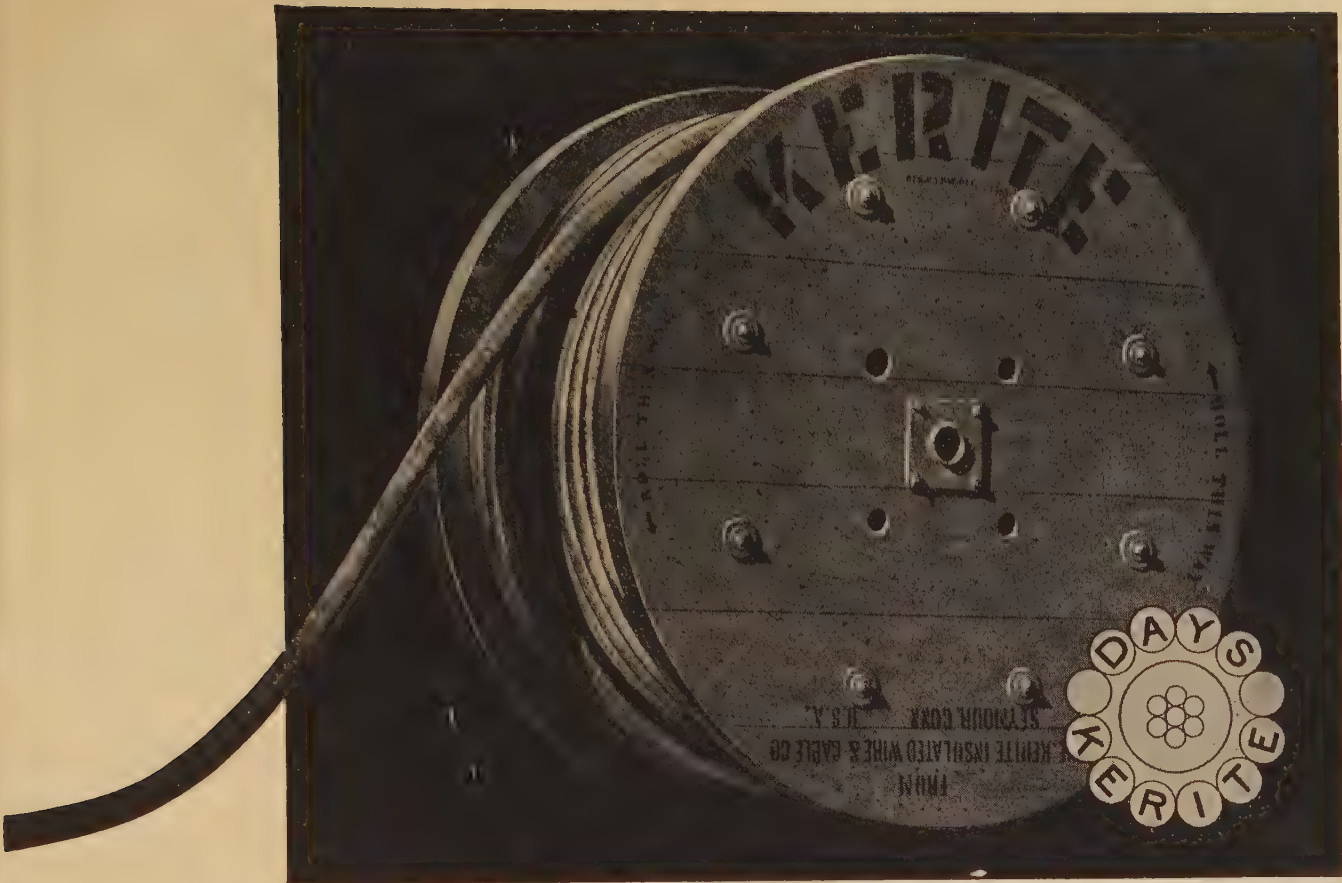
THE DETACHABLE CONNECTOR WITH NO LOOSE PARTS!

This is the test that wins *instant approval* for the Burndy LINKIT! For even 100 makes and breaks would be *extreme service* for a small wire connection. Yet the LINKIT withstands *thousands,* with little effect on conductivity . . . proof of the *enduring strength* and *wearability* of its four *long* contact surfaces.

Remember, too, that each half of the LINKIT is identical . . . and there are no extra parts to loosen or lose. The halves are quickly and securely indented to the wires with Burndy HYTOOLS in the usual way. LINKITS and LINKIT lugs are available for all wire sizes from #22 up. Literature gladly sent on request.

BURNDY ENGINEERING CO., INC., 107 Eastern Blvd., New York 54, N.Y.

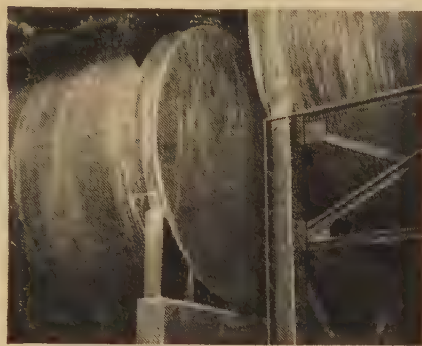
BURNDY *Headquarters for Connectors*


KERITE

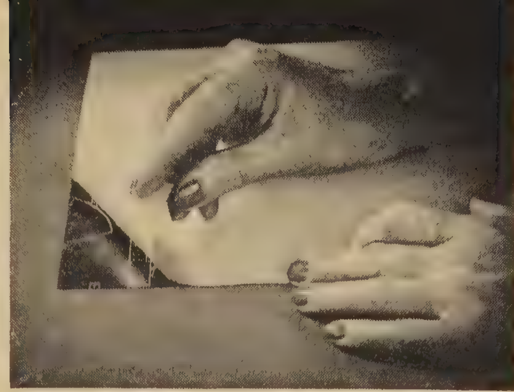
Survivors of sea casualties are easier to spot from a rescue plane now that both the Army and Navy are providing life jackets with special packets containing a "tea-bag" of fluorescent dye. In times of emergency the dye is readily released to spread quickly over a large area of the sea. One problem in the manufacture of these packets was to find a coating and an adhesive that were strong, waterproof, and that would protect the dye at extreme temperatures. The answer was found in a VINYLITE Plastic. Cloth is coated, then instructions are printed on it. The "tea-bag" is inserted and the packet edges are heat-sealed, using the coating as the adhesive.

A manufacturer of machine roll coverings experimented with an extruded tube of VINYLITE Elastic Plastic applied over the metal core. But, with vulcanizing impossible, how could this new material be successfully bonded? The answer came in the form of an adhesive made with VINYLITE Resins. And because VINYLITE Plastics do not oxidize the new platen is proving to be superior to its rubber predecessor.

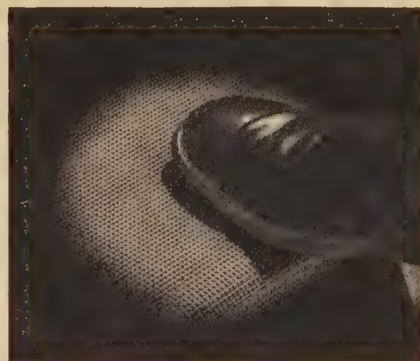
These two storage tanks contain concentrated sulfuric acid. The temperature of the metal is usually between 90 and 100 degrees F. There is considerable spillage. Under these conditions, ordinary coatings broke down in less than a month. One year ago the tanks were refinished with VINYLITE Resin VMCH air-dry coating. Today, although stained by spilled acid, this coating is unharmed.



Tiny grommets must be made by the thousands to supply the needs of American military planes and trucks. Today these grommets are injection molded of a VINYLITE Elastic Plastic. This conversion saves large quantities of critical rubber, but even more important, it cuts molding time to a fraction of that formerly needed. Scrap and trim can be remolded immediately without costly reprocessing.



Important blueprints became oil stained, wrinkled, and torn in war-plant shops. How could they be made more durable to eliminate this waste? Now a thin sheet of VINYLITE Elastic Film, backed with paper, is laid over the print . . . then ironed on at moderate temperature. The thermoplastic film softens . . . adheres to the print. As soon as it cools the backing paper is stripped off, leaving a strong, glossy, water- and oil-proof coating.



Bomber floor mats and catwalk mats must be skidproof and unaffected by oil. A prominent rubber company, using standard rubber-processing equipment, calendered VINYLITE Elastic Plastics on a cloth base . . . embossed the surface for maximum traction. Since VINYLITE Elastic Plastic requires no curing or vulcanizing these sturdy, skidproof, oilproof mats are made at low cost.

PROPERTIES OF "VINYLITE" ELASTIC PLASTICS—These are a relatively new group of VINYLITE Plastics with rubber-like or elastomeric properties. They are produced in a variety of forms, ranging from soft to semi-rigid. They possess great toughness, and excellent resistance to continued flexing, and to severe wear and abrasion. Tensile strength is higher than that of most rubber compounds. Their electrical insulating properties are outstanding. They are not subject to oxidation. By correct choice of plasticizer, they can be made non-flammable, and highly resistant to water, oils, and corrosive chemicals. They are available in a wide range of colors, either translucent or opaque, or can be supplied in their natural, colorless, transparent state. Since all VINYLITE Elastic Plastics are thermoplastic, no curing or vulcanizing is required. They are more affected by temperature changes than is rubber, but their operating range is wide, some types remaining flexible at —50 deg. F., yet tack-free at 200 deg. F.

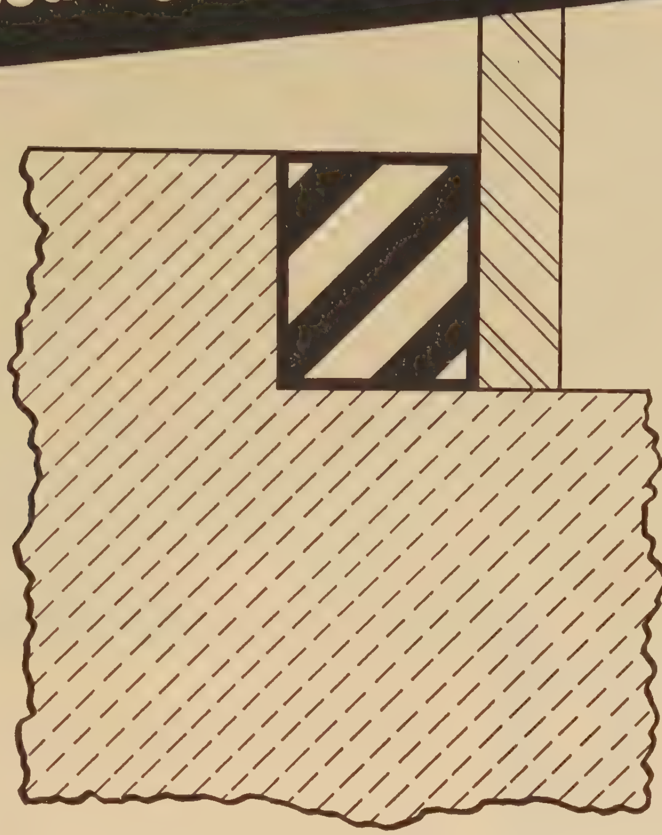
VINYLITE Elastic Plastics are supplied as sheeting and as compounds for calendering onto cloth, and for molding and extrusion.

PROPERTIES OF "VINYLITE" RIGID PLASTICS—Produced from unplasticized vinyl resins, VINYLITE Rigid Plastics possess a combination of properties found in no other thermoplastic material. Because of their extremely low water absorption, these plastics remain dimensionally stable under widely varying atmospheric conditions. They have exceptional resistance to alcohols, oils, and corrosive chemicals. They have high impact strength and tensile strength. They are odorless, tasteless, and non-toxic. They do not support combustion. They are available in a wide range of colors, translucent or opaque, and also in colorless, transparent forms. They are supplied as rigid sheets or as molding and extrusion compounds. Rigid sheets can be fabricated by forming, drawing, blowing, spinning or swaging, and can be punched, sheared, sawed, and machined on standard metalworking tools. Molding compounds are suitable for both compression and injection molding. Extrusion compounds give highly finished continuous rigid rods, tubes, and shapes directly from the die.

PROPERTIES OF "VINYLITE" RESINS FOR SURFACE COATINGS—Correctly formulated and applied, VINYLITE Resins yield finishes of unusual toughness, gloss, adhesion, and chemical resistance. They can be applied by spraying, knife-coating, or dipping to a wide variety of surfaces, such as metal, cloth, paper, and concrete. Prepared by dissolving resins in organic solvents, these finishes can be modified with a wide variety of pigments, dyes, and plasticizers. These resins are generally not employed with other film-forming bases, therefore, coatings formulated from them exhibit the desirable features of VINYLITE Resins alone. Drying is solely by evaporation of solvent, and finishes can be either air-drying or baking types.

PROPERTIES OF "VINYLITE" RESINS FOR ADHESIVES—Unusual toughness, resiliency, and impact resistance are characteristic of adhesives made of VINYLITE Resins. These resin adhesives are widely used as bonding agents for such materials as cellophane, cloth, paper, cardboard, porcelain, metal, mica, stone, leather, wood, and plastic sheets and film. They are available as powders for the compounding of adhesives, or as solutions sold under the trade-mark VINYLSEAL. The latter are especially recommended for bonding impervious materials, such as metals, and urea and phenolic plastics. Their bonding strength is comparable to that obtained with soft solder. By the addition of plasticizers, adhesives based on VINYLITE Resins can give almost any degree of flexibility desired.

Do you have a sealing problem like this?



REQUIRED: **A trouble-free seal between metal and porcelain. It should be a compressible, resilient, and noncorrosive material. It must not deteriorate.**

HERE'S THE SOLUTION:

Perfect sealing between metal and porcelain is assured with gaskets of Armstrong's Cork-and-Synthetic-Rubber Compositions.

The cork content makes these gaskets truly compressible and thus prevents cracking of the porcelain. Their exceptional resiliency maintains a tight seal, even between rough surfaces. It also takes up the differential in expansion and contraction of the two materials.

Special Advantages

No sulphur is used in compounding the Armstrong's Compositions which are recommended for this type of sealing job; therefore, they do not cause corrosion. They are free of extractables which would contaminate oil. They do not require an adhesive for assembly.

Armstrong's Sealing Service

For sound recommendations concerning *any* sealing problem you have, send complete details and an assembly print to Armstrong's engineers. They have developed more than fifty specialized sealing compositions. (See the list of general types below.) These materials are available as *die-cut gaskets, sheets, extruded rings, molded shapes, roll goods, ribbon, or tapes.*

Useful Booklet

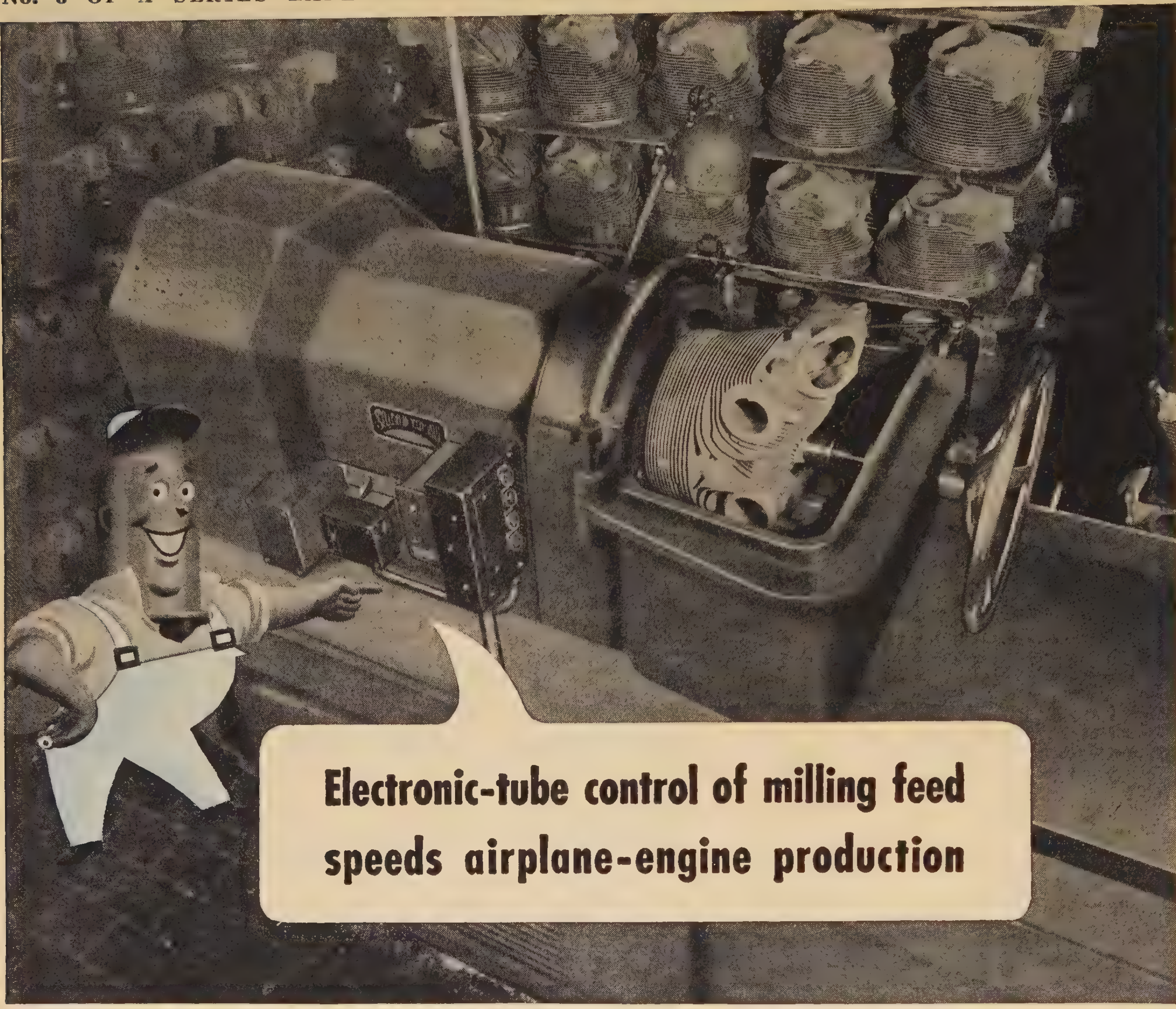
Helpful information about physical properties of sealing materials is presented in "Armstrong's Gaskets, Packings, and Seals." Send for your copy of this free booklet today. Write Armstrong Cork Company, Industrial Division, 4012 Arch St., Lancaster, Pennsylvania.

ARMSTRONG'S
GASKETS · SEALS · PACKINGS

Synthetic Rubbers • Cork-and-Synthetic-Rubber Compositions*
Cork Compositions • Cork-and-Rubber Compositions
Fiber Sheet Packings • Rag Felt Papers • Natural Cork

*FORMERLY "CORPRENE"

No. 8 OF A SERIES EXPLAINING THE USES OF ELECTRONIC TUBES IN INDUSTRY



BACK THE ATTACK! — BUY WAR BONDS!

How the General Electric thyratron acts as a synchronous switch and a power rectifier

THE Plan-O-Mill is a versatile machine tool for milling external and internal, right and left hand threads and forms. Here you see this equipment cutting threads on aircraft cylinder heads.

The General Electric Thy-mo-trol is standard equipment on the Plan-O-Mill. Thy-mo-trol is an electronic motor control unit that gives separate control of feed-in and feed-around.

It is a General Electric electronic tube, the thyratron, which makes possible the operation of Thy-mo-trol. The thyratron acts as a lightning-fast automatic switch, responding to and correcting load variations so that cutter speed stays constant. It is also a rectifier, converting alternating current into direct current.

Change of gears and sheaves are unnecessary in this motor control operation. Feed changes are automatic, and cutter speed remains constant regardless of variations in load. If the load limit is exceeded, motor control shuts the power off, protecting the feed mechanism.

It is the purpose of the G-E electronic tube engineers to aid any *manufacturer* of electronic devices in the application of tubes. Through nationwide distribution, G.E. is also prepared to supply *users* of electronic devices with replacement tubes.

FREE BOOKLET ON ELECTRONIC TUBES

We would like to mail you, without charge, an illustrated book entitled "How Electronic Tubes Work," written in easy and understandable language, and showing typical electronic tubes and their applications. Address *Electronics Department, General Electric, Schenectady, N. Y.*

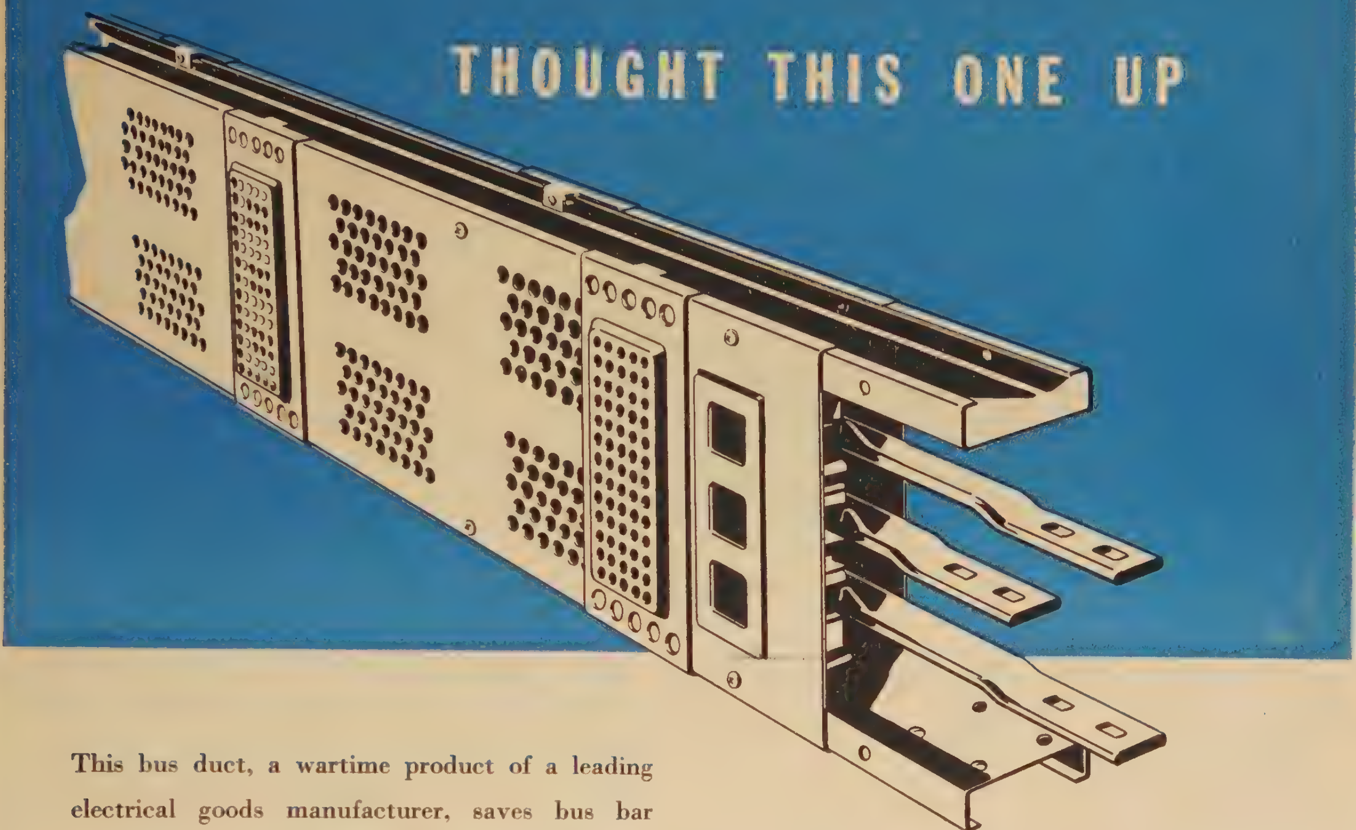
• Tune in "THE WORLD TODAY" and hear the news direct from the men who see it happen, every evening except Sunday at 6:45 E.W.T. over CBS. On Sunday listen to the G-E "All Girl Orchestra" at 10 P.M. E.W.T. over NBC.

THERE IS A G-E ELECTRONIC TUBE FOR EVERY PURPOSE

GENERAL ELECTRIC

162-B15-8850

A WARTIME IMAGINEER THOUGHT THIS ONE UP



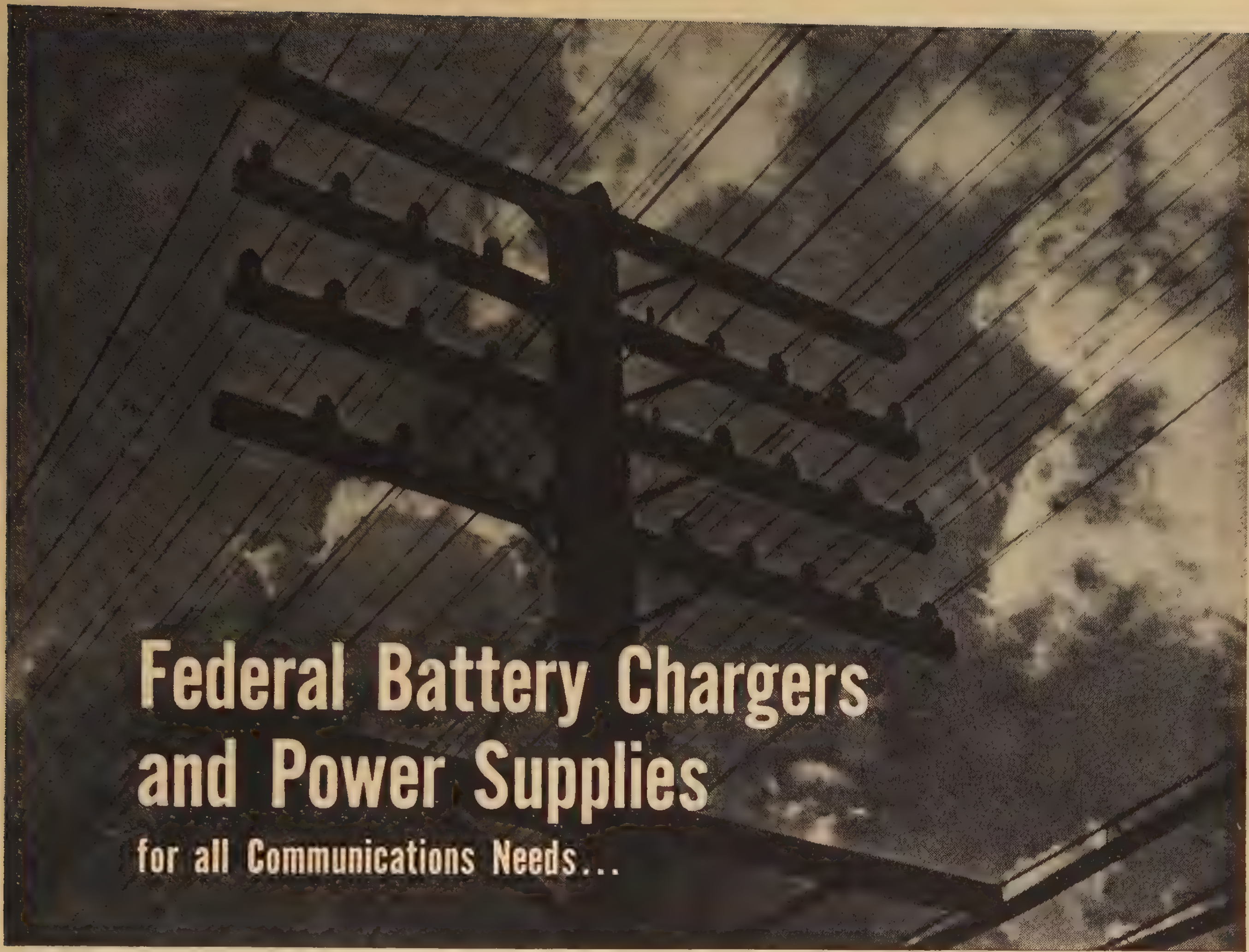
This bus duct, a wartime product of a leading electrical goods manufacturer, saves bus bar material by permitting heat to be dissipated faster; smaller bus cross sections can be employed for equal ratings. And it saves on the housing material by using perforated metal instead of solid sheet.

This is an excellent example of what we mean by *Imagineering.* Some engineer departed from standard channels of thinking, and produced a "substitute" which may have its effect on future construction.

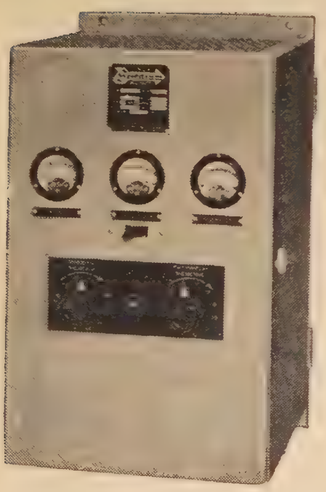
Now, when postwar designers say, "Make both the bus bar and the housing of Alcoa Aluminum," that will be plain, good *Engineering.* The weight savings effected by building with aluminum means less burden to be carried by supports. Aluminum bus bars have ample current-carrying capacity and aluminum housings provide additional electrical advantages.

Prewar power lines of A.C.S.R., Alcoa Aluminum bus bars and housings, are serving thousands of war industries—visible evidence that it pays to Imagineer with aluminum. ALUMINUM COMPANY OF AMERICA, 2149 Gulf Bldg., Pittsburgh, Penna.


IT'S THE
PORCELAIN
THAT INSULATES



. . . powered by I. T. & T. Selenium Rectifiers



Typical Automatic Noiseless Telephone Battery Charger

Built for highest efficiency and powered by long-life I. T. & T. Selenium Rectifiers, Federal Battery Chargers and Power Supplies are available in all types and sizes, in a wide range of ratings, to meet every communications requirement — telephone, telegraph, signaling and alarm systems. The many types include automatic regulated noiseless chargers and battery eliminators, manually operated units and non-filtered types.

Federal Battery Chargers and Power Supplies provide important operating advantages: no routine maintenance; no radio interference; hum-free output; high overall efficiency. They are for use on commercial AC circuits; they are rugged and compact and have a minimum of critical materials.

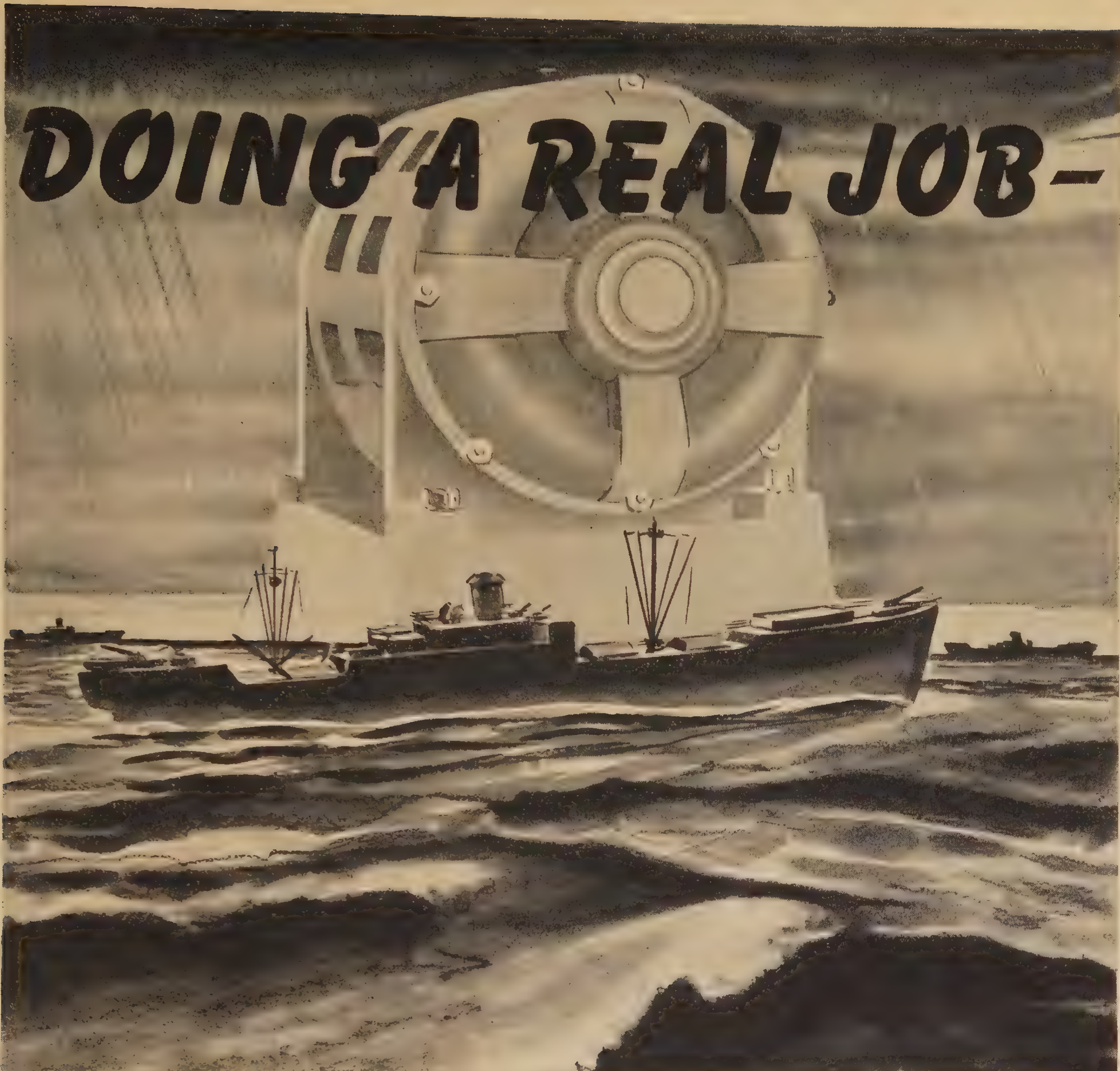
The "power unit"—the I.T.&T. Selenium Rectifier introduced and manufactured by Federal — is standard in the electrical field. Its freedom from moving parts, its wide temperature operating range, its capacity for overload and unlimited life, assure stable and satisfactory performance. *Consulting engineering services available from Technical Department.*

Federal Telephone and Radio Corporation

SELENIUM RECTIFIER DIVISION

AN IT&T ASSOCIATE

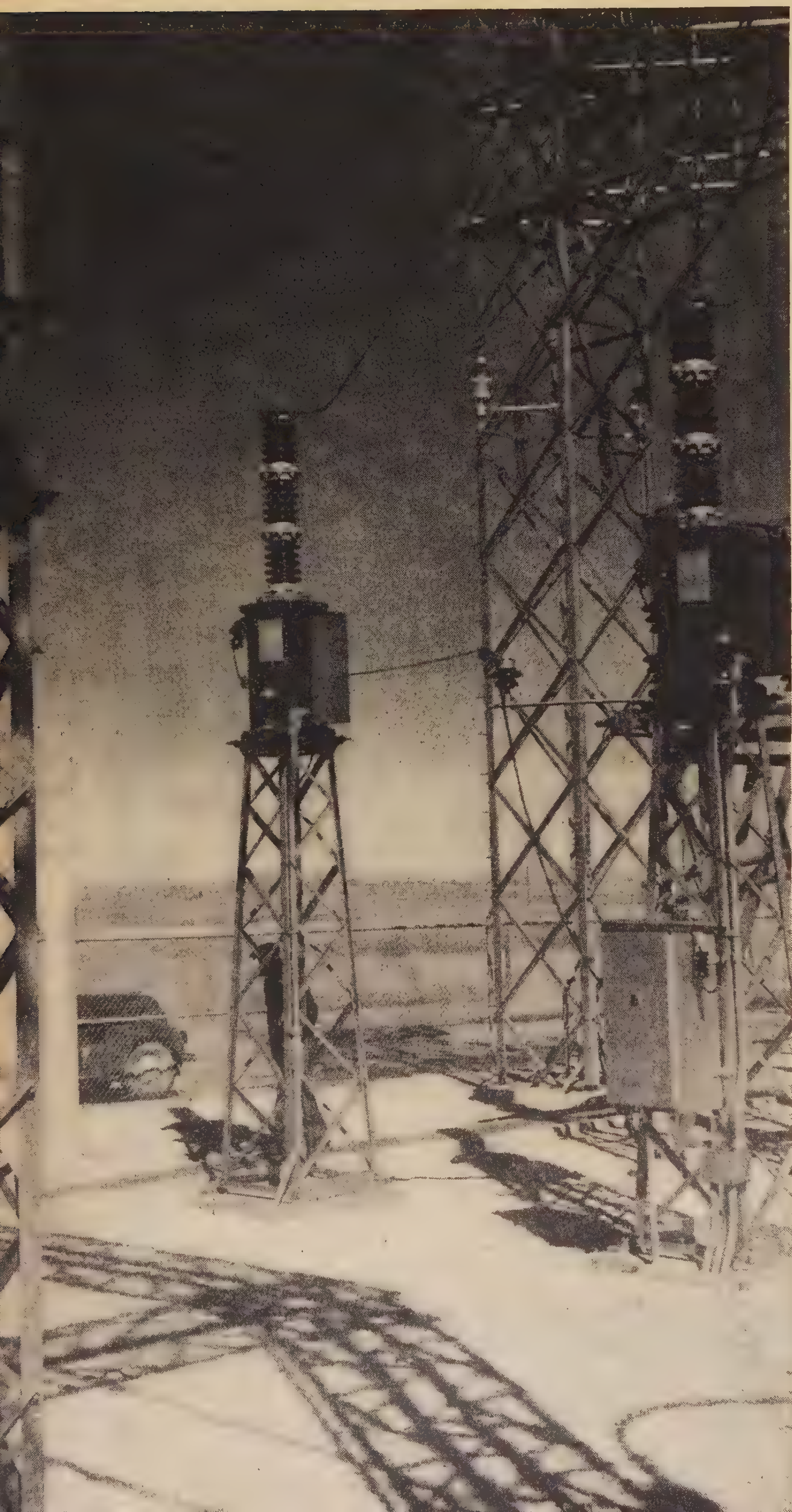
1000 Passaic Ave.
East Newark, New Jersey


DOING "A REAL JOB"—

PRESENT power systems

PLUS G-E carrier current

SAFELY deliver more kilowatt hours



MANY power companies—faced with increased power demands for war uses — can deliver more kilowatt hours and stay well within safe operating limits by installing modern G-E carrier-current equipment. Regardless of the type of carrier function needed, General Electric can supply it.

Besides offering all types of carrier-current equipment, an exclusive feature of G-E design is its package-type unit construction. For example, your immediate need may be pilot relaying to improve stability of transmission lines, increase their load-carrying capacity, and reduce outages in protected or unprotected sections. Later, when more functions are wanted, additional G-E apparatus is designed to fit quickly and easily into your original installation.

Literally "packaged" G-E carrier-current equipment can be added as needed for every purpose, if you insist on modern G-E equipment from the start.

Here are some major carrier-current functions that may boost the kilowatt-hour output of your system:

1. Pilot relaying
2. Transferred tripping
3. Automatic tie-line load control
4. Telemetering
5. Telephony
6. Supervisory control
7. Line sectionalizing control

G.E. has led the field in carrier-current design, research and application for 20 years, and its installations cover the nation. Your G-E representative will gladly discuss with you the application of carrier current to your system. . . . *Electronics Department, General Electric, Schenectady, N. Y.*

Tune in "THE WORLD TODAY" and hear the news direct from the men who see it happen, every evening except Sunday at 6:45 E.W.T. over CBS. . . . On Sunday listen to "The Hour of Charm" at 10 P. M. E.W.T. over NBC.

Transmitter-receiver assembly mounted on same column that supports the coupling capacitor: base also includes line tuning equipment and a potential device.

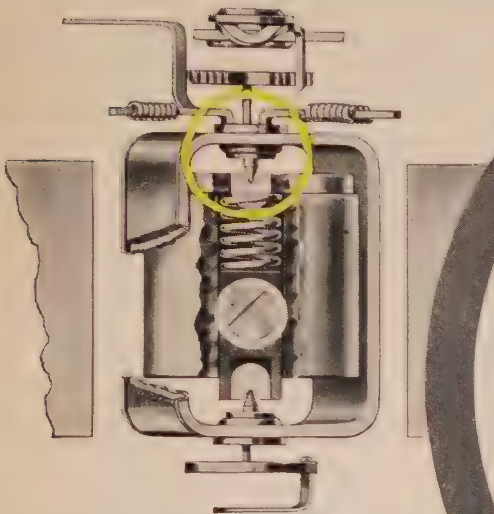
GENERAL ELECTRI

151-B4

New INTERNAL-PIVOT ELECTRIC INSTRUMENTS

2½-inch - - - 1 inch deep

For radio and other communications service; d-c voltmeters, ammeters, milliammeters, microammeters, and radio-frequency ammeters and milliammeters (a-c thermocouple type). Cases are brass or molded Textolite.



(Above) The new internal-pivot bearing construction. (Right) Top bearing (pivot and jewel) magnified 20 times. Note strong, solid construction.

WHY THIS BEARING CONSTRUCTION INSURES LONG-TIME SERVICE . . .

IN THESE new G-E instruments, the pivots are solidly mounted on the *inside* of the armature shell instead of being cemented to the outside of the armature winding. The result is a rigid construction that helps to maintain accurate alignment.

The steel pivots, highly polished, are of the aircraft type, larger than normal. This means less stress on the bearing surfaces and a construction that will stand rough treatment and shock.

The pivots rotate in low-friction, highly polished, glass vee jewels—one mounted rigidly in the top of the frame-and-core assembly, and the other mounted in a movable lower jewel sleeve located in the soft-iron core.

This combination—accurately formed, hard-glass jewels and large-radius steel pivots—provides a co-ordinated bearing that has proved, by field tests, to be excellent from the standpoint of long life and ability to withstand vibration.

Thin, Strong, Accurate Instruments

1. *Thinness* is obtained by solidly mounting the pivots on the inside of the armature shell. Most instruments are approximately one inch deep.
2. *Strength* is obtained by short, solidly mounted, large-radius pivots and the extra-strong over-all case.
3. *Sustained Accuracy* is insured by the featherweight moving element, combined with high torque and permanent alignment of all parts.

For ratings, prices, and dimensions, ask our nearest office for Bulletin GEA-4064, which covers instruments for use in radio and communications equipment; or Bulletin GEA-4117, which describes those suitable for naval aircraft. *General Electric Co., Schenectady, N. Y.*

D-c voltmeters, volt-ammeters and ammeters are specially designed to measure voltage and current in battery and battery-charging circuits on naval aircraft. They meet applicable Navy specifications.

SMALL PANEL INSTRUMENTS

GENERAL ELECTRIC

602-44-6200

Announcing

A NEW Dielectric Material for Capacitors-

LECTROFILM—a new product developed by the General Electric Laboratories—is a synthetic dielectric made from raw materials that are available in large quantities in the United States.

Capacitor manufacturers will find it ideal for use in most radio-frequency-blocking and by-pass, fixed capacitors that for years have been built with mica. These capacitors are of the type used in communications and other electronic equipment.

Lectrofilm has a greater combination of desirable mechanical and electrical properties than any other one capacitor dielectric material. It is available in both rolls and sheets, and can be used in present capacitor production lines, little or no change being required in equip or manufacturing methods. Its strength flexibility make it well suited to handlin automatic means.

Best of all, lectrofilm has uniform chara istics; it requires little if any grading, so or inspection. Therefore, it is economic well as easy to use, and when properly ap will cut down the number of finished capa that are rejected in test. Users of lectr can expect increased capacitor produ with present facilities without any increa man-hours.

Lectrofilm is available for use by manufact making capacitors for the armed forces.

LECTROFILM

TYPICAL CHARACTERISTICS

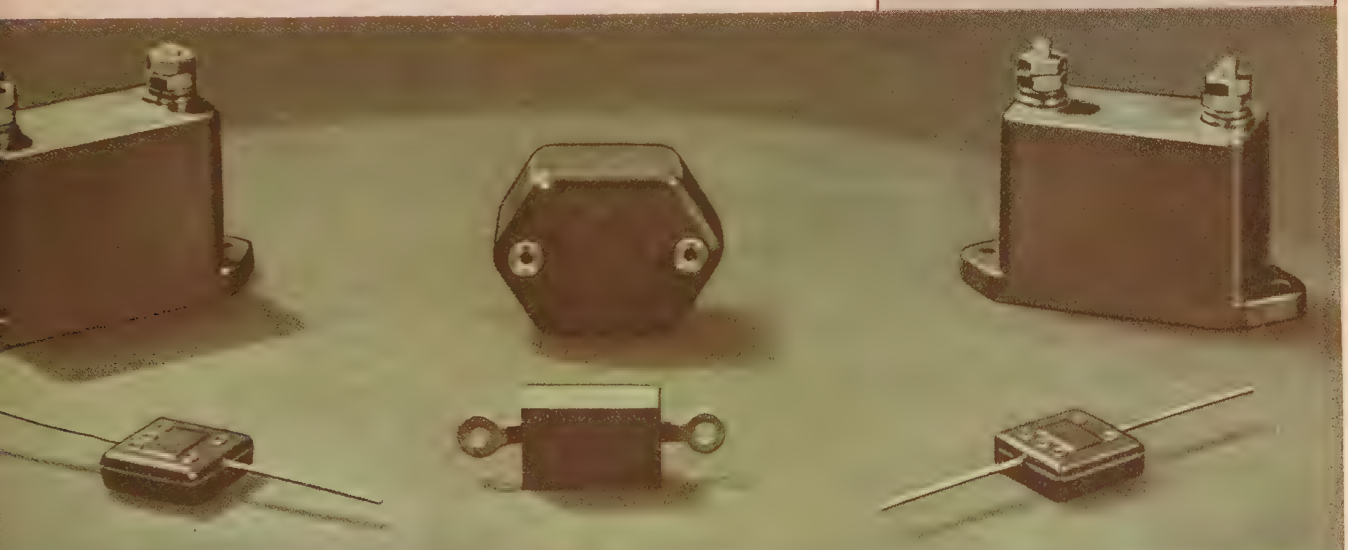
	Lectrofilm in Rolls, No. 2681	Lectrofilm in Sheets, No. 2682
D-c breakdown strength	1900 volts per mil (Two or more thicknesses)	2500 volts per mil
Dielectric constant	4.0 or more	5.5 or more
Tensile strength	Equal to Kraft capacitor paper	Equal to Kraft capacitor paper
Power factor at 1,000,000 cycles* Per cent at 25 C Per cent at 100 C	3.5 or less 1.75 or less	2 or less 1 or less
Capacitance temperature coefficient, per cent per degree C*	0.05 to 0 15	0.05 to 0.15
Maximum recommended operating temperature	100 C	125 C

hese characteristics, determined by actual test results on capacitors built with lectro-m, will depend on the type of capacitor construction.

For information on

- **sizes**
- **thicknesses**
- **weights**
- **additional characteristics**

...write for Bulletin GEP-217A. Address: Section 16-216, General Electric Company, Pittsfield, Mass.



Several types of communications capacitors built with lectrofilm

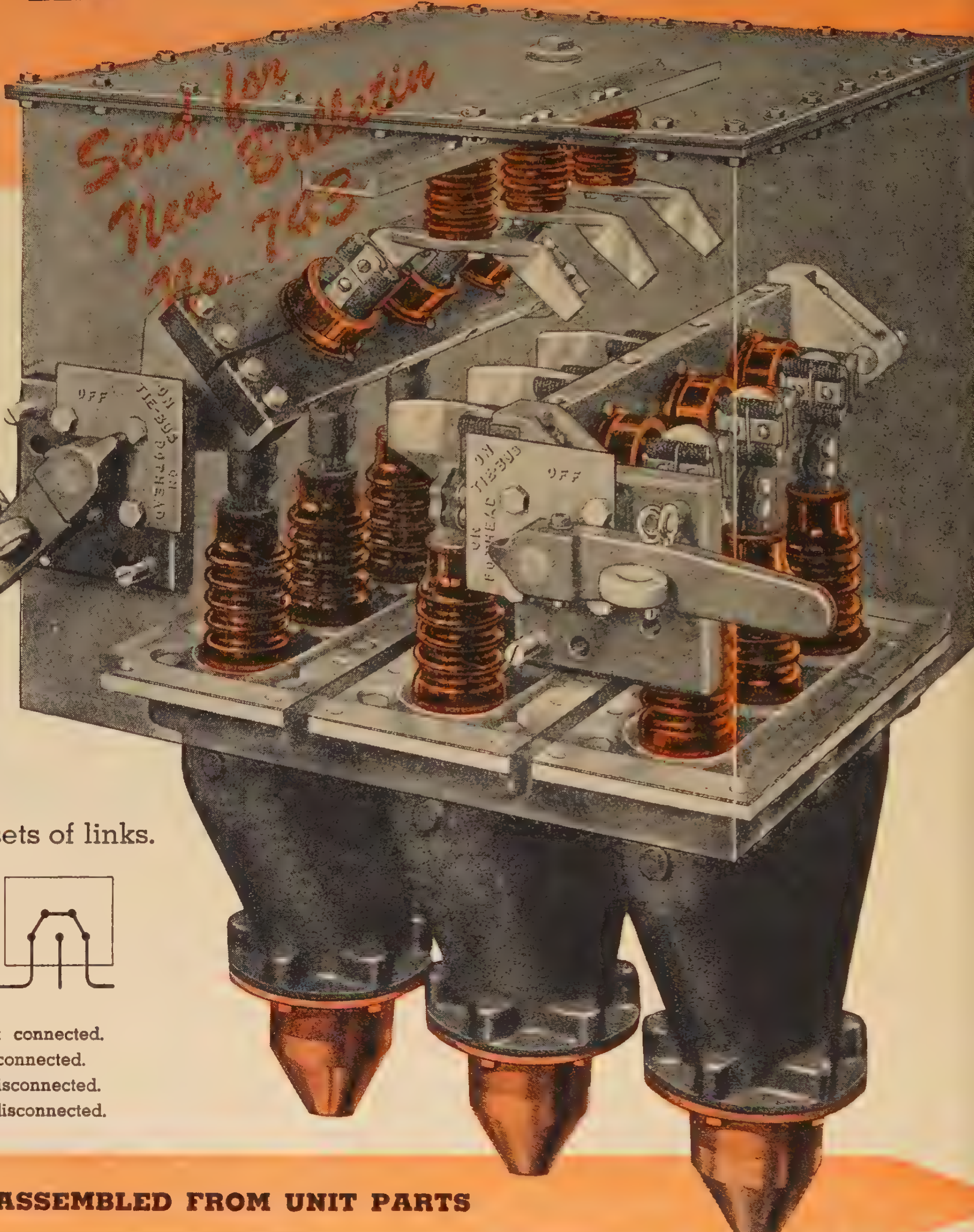
Every week 192,000 G-E employees purchase more than a million dollars' worth of War Bonds

GENERAL ELECTRIC

407-08-5700

Quickly isolate underground cable circuits with

G & W Type "RA" Subway Oil Disconnects



Simple and safe sectionalization by merely operating external lever arms.

No covers to remove.

No seals to be broken.

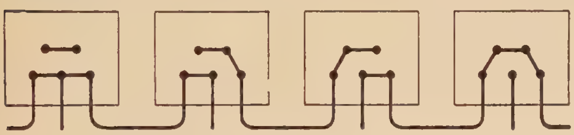
No links to unbolt.

Full 400 ampere load break with 7500 volt units—Up to 100 ampere load break with 15000 volt units—No load break with 23 Kv. and 34.5 Kv. units.

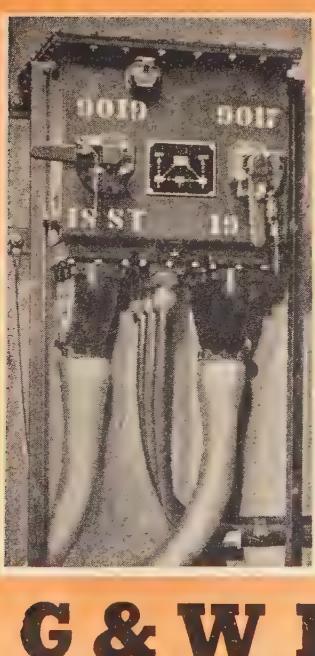
Oil filled tanks keep size to minimum—operating shafts perfectly sealed.

G & W TYPE "RAL"

4 different connections with 2 sets of links.

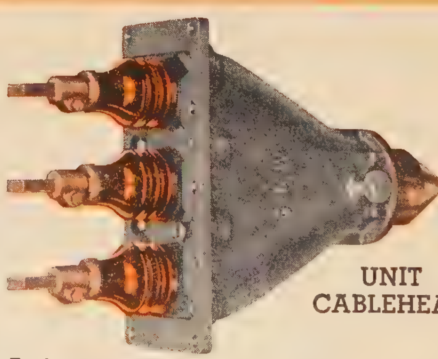


1. Loop or feed through with branch circuit connected.
2. Feed branch from left with other feed disconnected.
3. Feed branch from right with other feed disconnected.
4. Loop or feed through with branch circuit disconnected.



ASSEMBLED FROM UNIT PARTS

ROCKER ARM LINKS



UNIT CABLEHEAD

Tanks are welded in proper size and form and fitted with one, two or more rocker arm link units. Cableheads in straight or elbow shapes are attached to the bottom or to the bottom and sides of the tank to complete the assembly.

WELDED STEEL TANK

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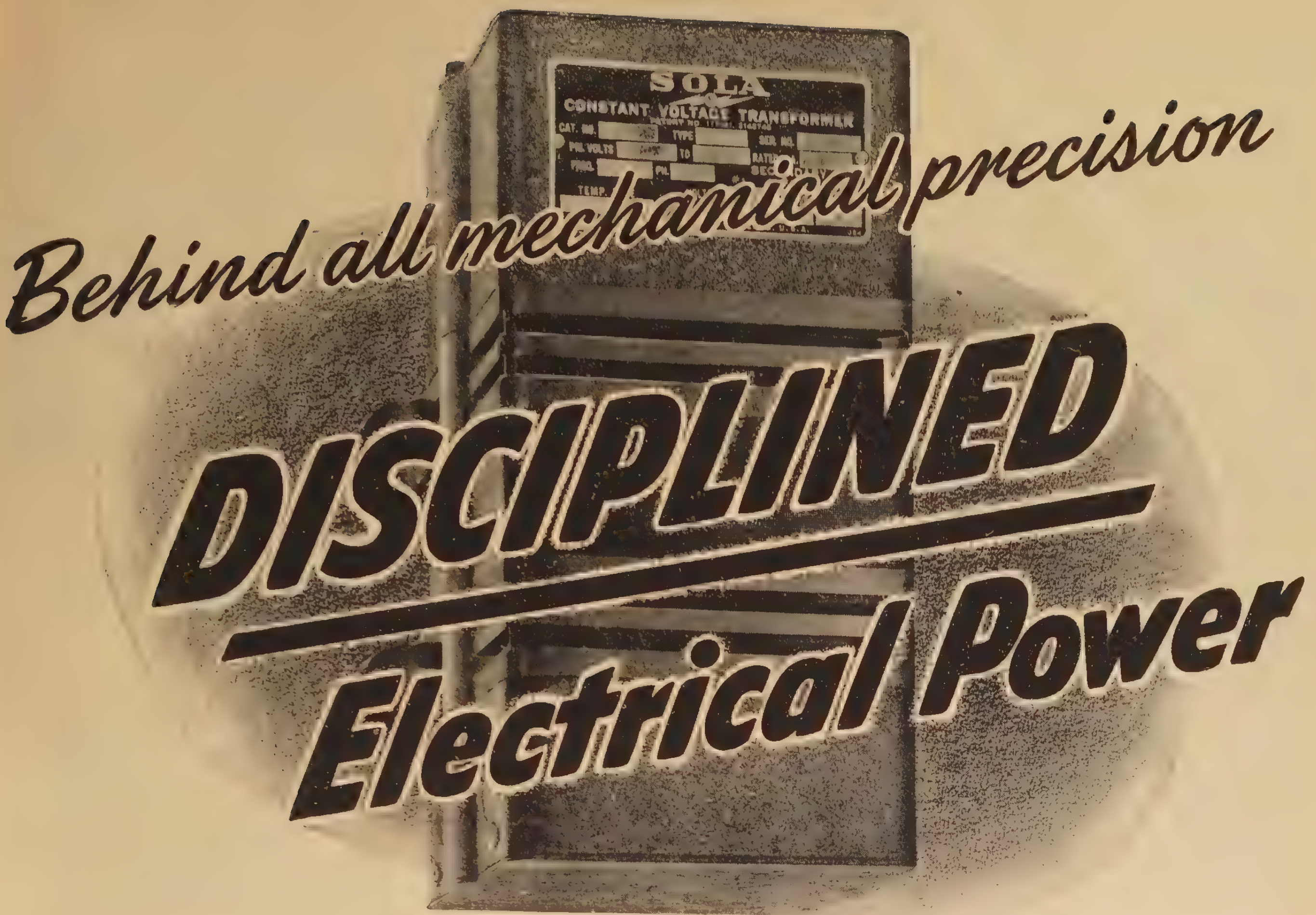

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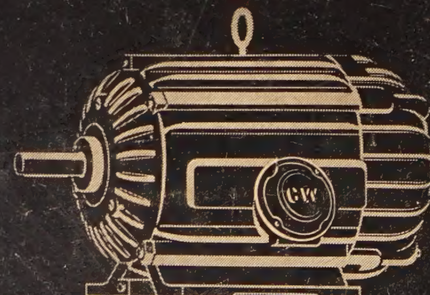
CROCKER-WHEELER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